ALLIS-CHALMERS Electrical REVIEW

4TH QUARTER 1960

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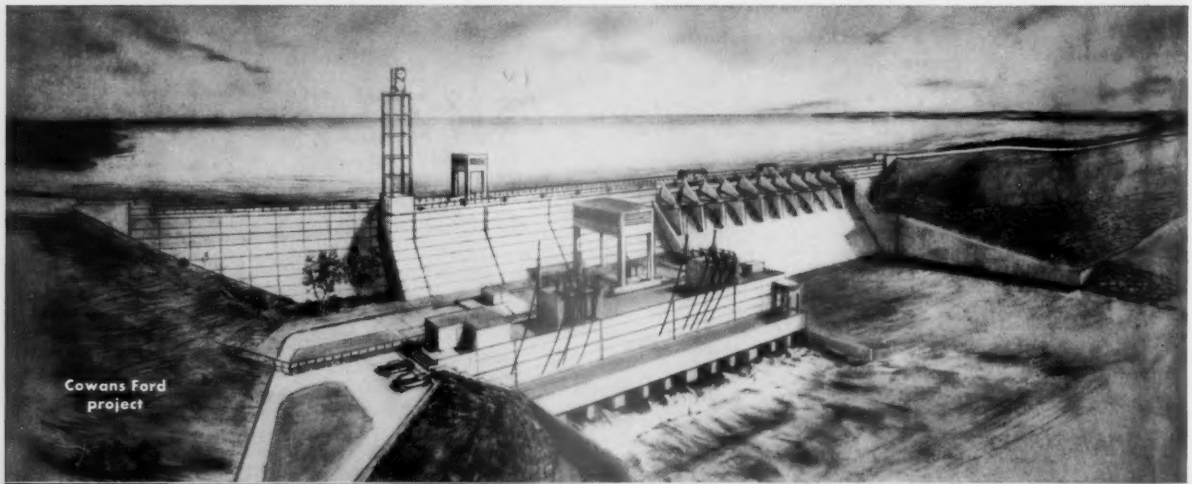
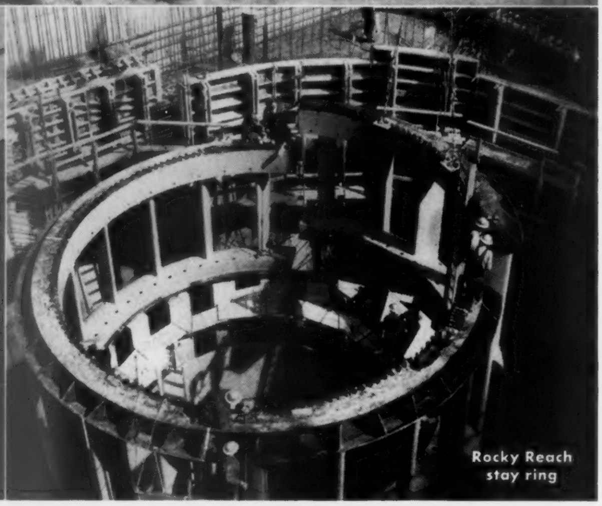


Cowans Ford project

Ice Harbor runner

Rocky Reach shaft and runner



Rocky Reach stay ring

high-head Kaplans for peaking

—previous record-breakers also by Allis-Chalmers

Cowans Ford is the outstanding illustration of Kaplan turbines used to provide peaking capacity. Duke Power Company, with Chas. T. Main Inc. as consulting engineers, has selected A-C Kaplan turbines for operation at 115-ft. head. Each of the three units is guaranteed to provide a maximum of 153,000 hp. Actual operation will involve only a short time each day, meeting peaking requirements of the system which is predominantly steam. The resulting average load factor, *less than 5%*.

KAPLAN RECORD-BREAKERS

Cowans Ford — the third in a series of outstanding A-C Kaplan turbine installations. These units exceed those at both Ice Harbor and Rocky Reach in rated capacity and head. All are similar in construction (those at Cowans Ford are slightly smaller in physical size). Allis-Chalmers diversified experience to date includes over 240 Kaplan turbines — *a record 6 million hp.*

Rocky Reach—public utility district supplied with seven Kaplan turbines, each 140,000 hp (under 92-ft. head).

Ice Harbor — Corps of Engineers installation recently provided with three new A-C Kaplans for operation at 89-ft. head. These held the record with 143,000 hp per turbine until Cowans Ford.

In every facet of hydro generation, people count on record performance and experience from Allis-Chalmers. For assistance anytime, contact your local A-C representative or write to **Allis-Chalmers,** Hydraulic Division, York, Pa.

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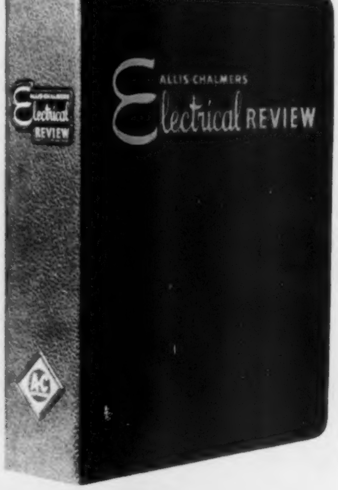

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ALLIS-CHALMERS

Electrical REVIEW

THE COVER

NEW SAFETY offered in 2.5 and 5-kv fused motor controls using control center type design. The control carriage, which includes the main contactor with power and control fuses, can be completely withdrawn from the cubicle. Shutters over the stationary portion of the disconnects protect personnel from dangerous high voltage leads.

The articles, "5-kv Motor Control Center Introduced" on page 10, and "50,000-Kva Interrupting Capacity in Half the Space" on page 12 tell of interesting new control concept.

Allis-Chalmers Staff Photo by Frank Hart

Allis-Chalmers
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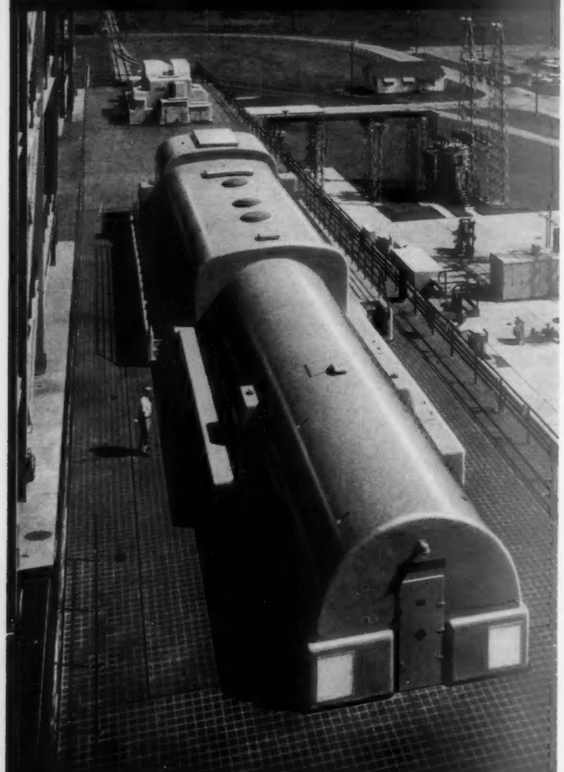


RECENTLY INSTALLED 265-mva unit is typical of modern turbine-generators being designed expressly to meet new 1960 TIF weighting curve. Increase in TIF in certain machines can be offset by special design feature.

by **L. T. ROSENBERG**
Thermal Power Department
Allis-Chalmers Mfg. Co.

Recent adoption of Telephone Influence Factor (TIF) weightings, reflecting the broadening sensitivity range of the modern telephone, has focused attention on the voltage wave form of turbine-generators.

THE VOLTAGE WAVE of today's large turbine-generators approaches the proverbial designer's dream of a perfect sine wave, perhaps more closely than that of any other type of rotating machine. Some of the features that contribute toward this favorable comparison are inherent because of physical considerations. Others are purposely introduced, even at considerable expense, to provide as nearly perfect a wave form as possible.

The new TIF weightings were adopted by the Joint Sub-committee on Development and Research of the Edison Electric Institute and Bell Telephone System to take into account improved frequency response of modern telephone equipment.

The presence of harmonic voltages or currents on power systems was recognized as a source of objectionable noise in exposed telephone circuits well over 40 years ago. Since then, in parallel with advances in generator design techniques, great strides have been made in telephone equipment as well as in the art of transmission of voice messages. Developments in power system transmission and distribution have also aided in this progress.

Experimental research beginning in 1914, led to the adoption of the first standards intended to control telephone noise from power lines. The studies disclosed wide differences in the potential disturbing properties of different frequency harmonics. The characteristics of the human ear as well as those of the telephone equipment then in use, were taken into account in these early experiments. The results of these tests were expressed as a curve called the "message weighting curve" giving the relative disturbance effect of the various harmonics in the telephone circuit as a function of their frequencies.

TIF curve developed

It was recognized that the inductive coupling between power and communication circuits is also dependent upon frequency. Assuming that the coupling is directly proportional to the frequency, a second curve, known as the "telephone interference factor weighting curve," was derived and published in 1919[1] expressing the potential disturbing influence of a given voltage or current on the power system as a function of its frequency. As shown in Figure 1, curve A, there is a peak at 1100 cps, due to the nature of the telephone equipment at that time.

1. See bibliography.

With the TIF weighting curve as a starting point, it was then possible to calculate the "telephone interference factor" of a complete voltage wave, defined as the ratio of the square root of the sum of the squares of the products of the individual harmonic voltages by their respective weighting factors, divided by the rms value of the voltage wave. The same definition may be applied to a current wave.

This definition and the 1919 curve were generally adopted by both utilities and machinery manufacturers as a convenient yard stick in the appraisal of any voltage or current wave with respect to its potential influence upon a telephone circuit. Acceptable telephone interference factors were then established for various types and sizes of electrical machines based on performance records in conjunction with TIF tests compiled on a great many machines. These acceptable values of TIF were later incorporated in Rotating Machine Standards, ASA C50.

In 1935, based upon further progress in telephone equipment, a new curve, B, was proposed[2] extending the frequency range to 5,000 cps. A new term, "telephone influence factor," was also proposed with this new curve as a distinguishing means from the earlier term. The abbreviation would still remain TIF, and the word "influence" was considered more appropriate than "interference." Although the 1935 curve was adopted soon afterward by telephone engineers and some utilities, it was not until 1955 that it was officially included in the ASA Standards for Rotating Machines, ASA C50.1.

New curve adopted

In the meantime, further experiments and more progress in telephone equipment toward improved uniformity of response to all frequencies gave rise to further refinements in the message weighting curve, placing increased emphasis on the higher frequencies and reducing the weightings of the lower. These investigations led to curve C, which was based on a message weighting curve developed in 1941 for the so-called "302 series" telephone hand set. The sharp peak near 1100 cps was substantially overcome.

Still more recently, the new "500 type" telephone set was introduced and is rapidly replacing earlier equipment. A working group on TIF Revision was appointed by the Joint Subcommittee on Development and Research of the Edison Electric Institute and the Bell Telephone System to bring the TIF standards up to date. Tests on the "500 series" set, carried out by the Bell Telephone Laboratories led to a new message weighting curve, and the corresponding TIF weighting curve, D. This curve, which was recently approved for adoption by the Joint Subcommittee, is now known as the 1960 TIF weighting curve. These developments are fully described in a recent AIEE paper by Messrs. W. C. Ball and C. K. Poarch.[3]

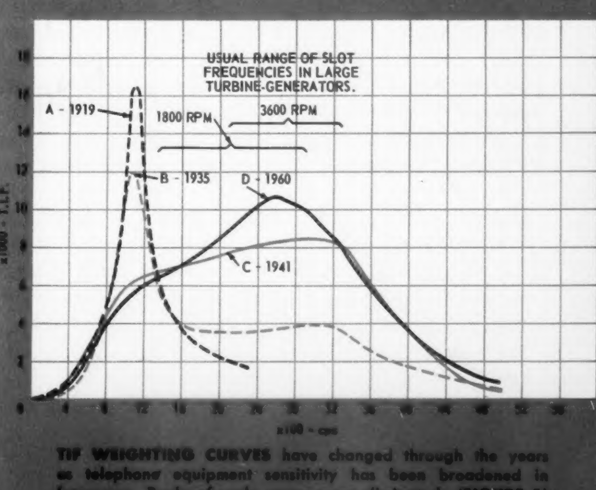
The 1960 weighting curve assigns higher TIF values to frequencies above 1300 cps than the 1935 curve now recognized in the ASA standards. Because of its inherent design requirements, the high speed turbine generator is more likely to possess slot frequency harmonics above this frequency than other types of alternators. The range of slot frequencies in turbine-generators are indicated in Figure 1. Unless additional precautions are taken to offset the increased weightings of these high harmonics, the telephone influence factors of turbine-generators may in some cases increase when computed on the basis of the 1960 curve. Table I shows the comparison of TIF values by both the 1935 and 1960 weightings on four typical turbine-generators. Even though an appreciable increase does occur on the 9375-kva machine, there still remains a generous margin below the acceptable ASA-TIF values given in the last two columns.

Harmonics stem from geometry

The turbine-generator has three important advantages over the salient pole machine insofar as its voltage wave form is concerned. It has a large air gap, a cylindrical rotor, and a distributed rotor winding. Although these factors account for the relatively low TIF values of turbine-generators, they cannot be said to produce a perfect sine wave, of voltage. Economic and engineering considerations prevent sinusoidal distribution of the rotor slots. As a result, the no-load flux density wave in the air gap is approximately trapezoidal with steps in the trapezoid resulting from the finite number of slots. Such a wave is composed of a fundamental plus odd harmonics.

The higher frequency harmonics, which have a direct bearing on the 1960 TIF's in turbine-generators are due mainly to the armature slots. As the rotor pole center moves from a stator slot to a stator tooth, any variation in air gap permeance will cause a pulsation in the flux at a frequency equal to the number of slots traversed per second by a fixed point on the rotor. To satisfy symmetry requirements, two odd multiples of the fundamental frequency appear in the voltage wave. The order of these harmonics is equal to the number of slots per pair of poles plus or minus one.

Saturation may also create harmonics in the wave form of a turbine-generator. The highest densities in the magnetic circuit usually occur at the tips of the stator teeth and the roots of the rotor poles and adjoining teeth. As these restricted areas saturate, the center portion of the flux density wave is further depressed. The presence of damper windings, balance screw holes and ventilating slots are also potential sources of harmonics in the open circuit voltage wave.



TIF WEIGHTING CURVES have changed through the years as telephone equipment sensitivity has been broadened in frequency. Peaks of early curves were eliminated. (FIGURE 1)

The limiting values of the telephone influence factor of rotating machines given in the ASA standards apply to the open circuit voltage wave. The addition of load current will materially affect the air gap flux distribution as the stator mmf wave is superimposed upon the rotor mmf wave. The effect of load on the harmonics in salient pole machines is treated in a recent paper by Messrs. David Ginsberg and Alois Jokl.[4] In practice, however, certain types of load may introduce appreciable harmonics of their own or even suppress some of those of the generator. Consequently, TIF measured under loaded conditions is hardly a reliable criterion of the generator wave form.

Design features that reduce TIF

The voltage wave generated in a single, full pitch coil in the armature will correspond very closely to the flux density wave in the air gap. Two adjacent coils δ electrical degrees apart, when connected in series will generate the vector sum of the voltages in each coil. The fundamental voltages will be displaced by δ electrical degrees and their vector sum will be nearly equal to their algebraic sum. The kth harmonic voltages will be displaced $k\delta$ electrical degrees, and their vector sum will generally be much smaller than their algebraic sum.

In a typical 3 phase winding as shown in Figure 2, there are several slots in a phase group, spanning a total of 60 electrical degrees. For this arrangement, the belt differential factor, by which the sum of the fundamental voltages is multiplied, is 0.955. The sum of the 3rd harmonic voltages on the other hand, is multiplied by a factor of 0.637 and that of the 7th by 0.136. Expressions for the reduction factors from this source, as well as for those from other sources are tabulated in Table II. The belt differential factors do not apply to harmonics caused by flux of fundamental space distribution, pulsating or rotating at k times the fundamental frequency.

As stated above, the slot frequency harmonics of a turbine-generator are the most likely to raise the TIF as determined on the basis of the 1960 weighting curve. The most direct solution is to eliminate the pulsations in permeance by careful attention to the design of slotting of both rotor and stator. Here again, such considerations as efficiency, temperature rise, tooling, drawings and manufacturing tolerances, often dictate optimum proportions that deviate from the ideal slotting arrangement. In such cases, an equally effective method of reducing slot harmonics consists of providing a helical skew in the stator slots. This is accomplished by skewing the dovetails as they are bolted to the inner periphery of the stator yoke before the core is stacked. Calculation by the formula in Table II indicates that the slot harmonics are virtually eliminated by skewing the stator core one slot pitch from one end of the stator to the other. The helical skew also serves to reduce space harmonics present in the flux distribution wave. Even though the full slot pitch of skew complicates the drawings, tools, and assembly processes nevertheless the wave shape benefits derived from it often justify the added manufacturing cost. Stator cores of many large turbine-generators are skewed.

The use of short pitch stator coils is another effective means for controlling harmonics. In generators transmitting their power at their own terminal voltage with their neutrals grounded, the triple frequency harmonics were sometimes found to induce telephone noise. The ⅔ pitch stator coil, which eliminates all triple harmonics, has been widely used to guard against difficulties from this source. With most modern large generators employing the unit system, no power is transmitted at generator voltage. Moreover, the neutral is usually grounded through a distribution transformer with resistor loaded secondary which behaves as a very high resistance ground connection. The triple frequency harmonics are less likely to be troublesome than some of the non-triples. The ⅔ pitch stator coil has no effect upon the percentage of the balanced harmonics, since they are reduced by the same pitch reduction factor as the fundamental. Optimum pitch from the standpoint of generator efficiency and capacity is usually in the neighborhood of 83 percent of a pole pitch. The triples are reduced by a factor of about 0.70 and certain undesirable non-triples such as 5th and 7th are effectively minimized.

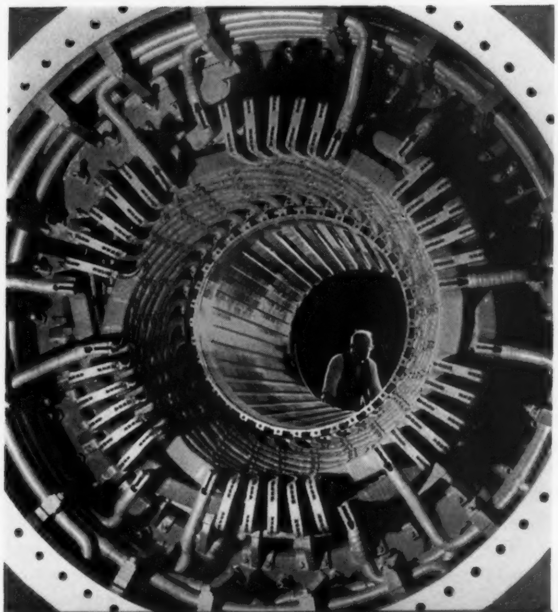
To permit optimum pitch, the triple harmonics in the flux density wave can be suppressed by making the pole center between rotor coils equal to ⅓ of the pole pitch. This arrangement also permits economical use of rotor space if the magnetic and conductor materials are properly proportioned. In practice, owing to the larger effective gap in the region of the rotor slots than in the pole region, the pole is made slightly wider than ⅓ of the pole pitch, to reduce the triple harmonics effectively.

Another improvement in the flux density wave form, arises from the use of one or two pairs of shallow slots adjacent to the pole center. These shallow slots have fewer turns than their deeper neighbors, so that a more nearly sinusoidal mmf wave is produced. This feature is made possible by the geometry of the sharply tapered pole center which justifies the exchange of copper for iron in the region of highest flux density.

Developments foster better communication

Progress in the art of telephone communication, broadening the sensitivity range of the equipment, has laid down a challenge to the manufacturers of turbine-generators. By fully utilizing advances in generator design techniques, the increased weightings in the region of turbine-generator slot frequencies are being met with a wide margin of safety, as indicated in Table I.

Progress in power line transposition and greatly increased use of generators tied directly to their own transformers have appreciably reduced the inductive influence of the power system on telephone communication. Moreover advances in longitudinal balance and shielding of modern telephone circuits have minimized their susceptiveness to outside disturbances. These factors are contributing in generous measure toward further progress in clarity and freedom from noise in today's most vital communication facility, the telephone.



SLOT ARRANGEMENT and skew of 3600-rpm, 2-pole, 150-mw, fully-supercharged, hydrogen-cooled turbine-generator was designed with TIF weighting curve in mind. **(FIGURE 2)**

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TABLE I

Comparison of TIF Values of Turbine-Generators by 1935 and 1960 Weighting Curves.

		1935 TIF		1960 TIF		ASA Std. (1935 Curve)	
Kva Rating	**Voltage**	**Balanced***	**Residual†**	**Balanced***	**Residual†**	**Balanced***	**Residual†**
265,000	20,000	3.39	5.60	3.88	4.60	50	30
206,470	18,000	3.70	7.00	3.93	5.48	50	30
32,000	13,800	11.30	4.30	10.10	3.80	50	30
9,375	13,800	17.30	14.20	33.70	19.10	60	30

* Harmonics other than triples

† Triples only

TABLE II

Harmonic Reduction Factors

Distribution Factor (Integral number of slots per pole) $C_{dk} = \sin\left(kc\frac{\delta}{2}\right) \Big/ c\sin\left(k\frac{\delta}{2}\right)$

Distribution Factor (Fractional number of slots per pole) $C_{erk} = \sin\left(kc\frac{\delta}{2}\right) \Big/ y\sin\left(k\frac{\alpha}{2}\right)$

Pitch Factor $C_{pk} = \sin\left(\frac{kp\pi}{2}\right)$

Skew Factor $C_{sk} = \sin\left(\frac{k\lambda}{2}\right) \Big/ \left(\frac{k\lambda}{2}\right)$

Connection Factor $C_{ck} = \dfrac{\sqrt{(\Sigma \sin k\theta_r)^2 + (\Sigma \cos k\theta_r)^2}}{n}$

Rotor Slotting Factor* $C\gamma_k = \sin\left(k\gamma'\frac{\pi}{2}\right) \Big/ k\gamma'\frac{\pi}{2}$

where k = Order of the harmonic to be calculated. (i.e. 1, 3, 5, 7, etc.)

c = Number of slots per phase belt

δ = Electrical angle between adjacent slots

p = Per unit coil pitch. (Expressed as decimal fraction of full pitch)

λ = Electrical angle of skew. (Measured at opposite ends of the core)

θ_r = Displacement angle of phase r

n = Number of phases in series between lines

$y = x \times$ [Actual number of slots per phase belt. (e.q. 2.73)]

$= \frac{1}{\phi} \times$ [Numerator of number of slots per pole expressed as an improper fraction reduced to lowest terms (e.q. 64, for $9\frac{1}{7}$ slots per pole $= \frac{64}{7}$)]

x = Denominator of fractional slot number reduced to lowest terms (e.q. 7, for $9\frac{1}{7}$ slots per pole)

ϕ = Number of phases

$\alpha = \frac{\delta}{x}$ = Minimum electrical angle between corresponding slots under different poles

$\gamma' = K_{rs} \times \left(\frac{\text{slotted arc of rotor}}{\text{total circumference}}\right)$;

(e.q. $\gamma' = K_{rs} \times \frac{20}{26}$ for 20 slots spaced $\frac{1}{26}$ circle)

K_{rs} = Carter Coefficient for rotor $\left(= \frac{\text{effective gap due to rotor slots}}{\text{net actual gap}}\right)$

* Ratio of the kth harmonic in a trapezoidal wave to the kth harmonic in a rectangular wave of equal height.

HIGHER HYDROGEN PRESSURES FOR SYNCHRONOUS CONDENSERS

by **H. H. ROTH**
Motor-Generator Dept.
Allis-Chalmers Mfg. Co.

Higher hydrogen pressures provide greater economy and reduced size for synchronous condensers. The design is dictated by the application.

PRESENT AMERICAN STANDARDS for hydrogen cooled synchronous condensers provide for a normal kvar overexcited rating at a hydrogen pressure of ½ psig, with a 60 C temperature rise on the stator windings and an 80 C rise on the rotor windings. The standards also provide for an increased rating of 120 percent of the ½-psig rating when operated at 15-psig pressure, without exceeding the ½-psig permissible temperature rise.

Some hydrogen cooled condensers have been purchased on specifications calling for only the 15-psig rating. Instead of purchasing a 50,000-kva, ½-psig machine, a 48,000-kva, 15-psig condenser might be purchased, which is equivalent to a 40,000-kva, ½-psig condenser.

Standard hydrogen cooled condensers have an underexcited rating of 42 percent of the ½-psig overexcited rating. Hence a 50,000-kvar, ½-psig condenser has an underexcited rating of 21,000 kvar, while a 48,000-kvar, 15-psig condenser has an underexcited rating of only 16,800 kvar. This loss of underexcited capacity may be of no importance for some applications, such as a condenser which serves a large industrial load and is used only for power factor correction. However, when used for transmission line regulation, the lagging capacity of a condenser may be of considerable importance.

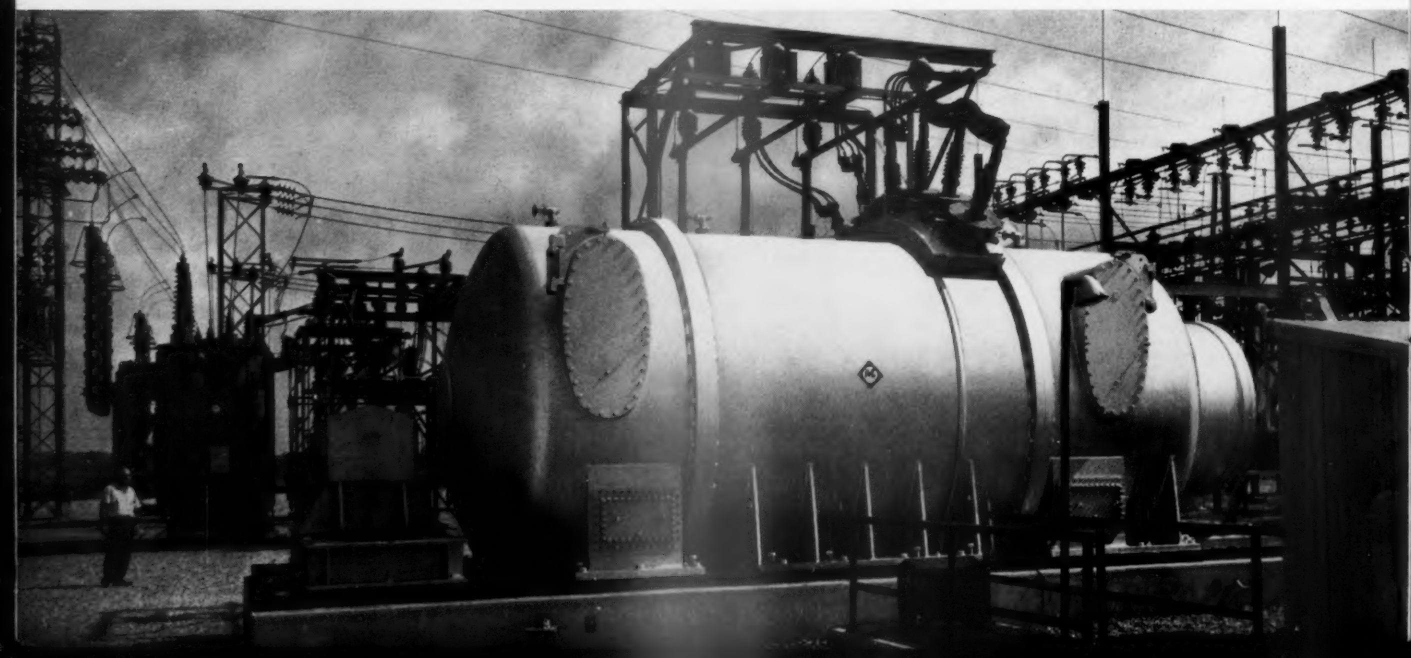
Higher pressures provide advantages

There are in operation today several condensers designed for operation at hydrogen pressures above the present 15-psig maximum. The condenser shown in Figure 1 is one of three 50,000-kvar, 30-psig units operating on one major power system. The advantages of using this higher pressure as compared to a ½-psig unit are:

1. Physical size of machine is reduced.
2. For a given kva a higher RPM can be used because the machine is reduced in size.
3. Foundation structure is smaller.
4. Less substation space is required.
5. Hydrogen volume is reduced.

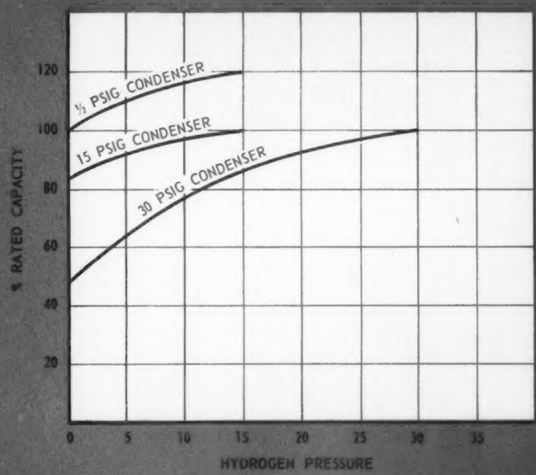
When compared to a 15-psig rated condenser, there is an additional advantage of full 42 percent underexcited capacity as compared to 33.6 percent for the 15-psig rated condenser, since both machines would be designed for the full normal underexcited capacity.

Condensers of conventional design do not readily lend themselves to direct cooling of the copper in the stator winding. It is therefore not possible to materially increase



the rating of an existing condenser designed for ½-psig normal operation over its 15-psig rating merely by increasing the hydrogen pressure to 30 psig. The thermal barrier of the stator coil insulation prevents any substantial increase in the heat transfer from the armature copper to the stator core and to the cooling gas. Excessive copper temperatures will result. A condenser for 30-psig operation must therefore have its stator winding designed for about the same current density as for machines built for lower hydrogen pressures.

A synchronous condenser designed for rated kvar at 30 psig may be operated at reduced hydrogen pressures, but its capacity will be reduced. The relative capability of the three machines with respect to hydrogen pressure is shown in Figure 2.



HIGHER PRESSURE synchronous condensers can be operated at reduced hydrogen pressures with a reduction in capacity. **(FIGURE 2)**

Supercharged cooled condenser in use

An interesting condenser application is the machine shown in Figure 3. This unit was built as an experimental completely supercharged generator with both rotor and stator copper cooled by direct contact with the cooling gas. While it was given a nominal rating of 47,000 mva at 30-psig pressure, 3600 rpm, it was designed for operation at pressures up to 75 psig.

An extensive testing program was carried out at the factory on this machine, from which much valuable data was obtained for use in the design of large completely supercharged turbo generators. Upon completion of this program the machine became available for sale, and was applied as a synchronous condenser in a large steel plant.

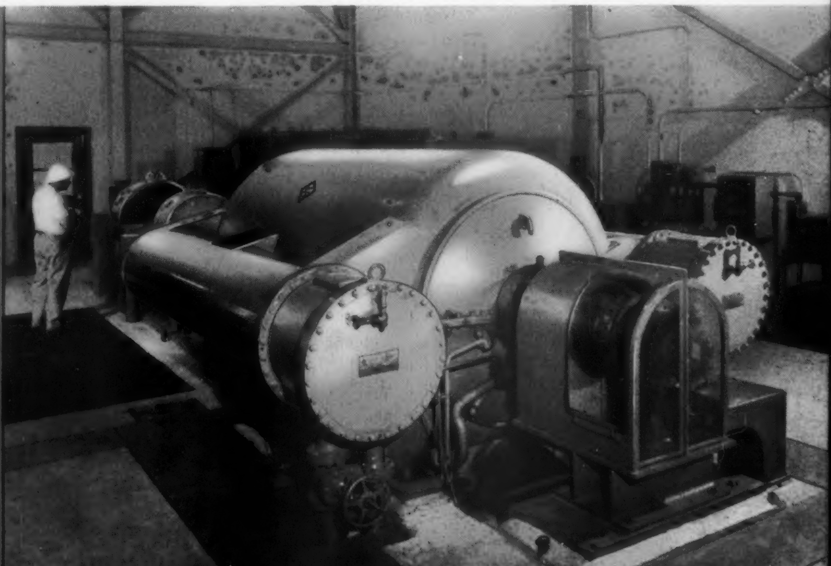
This condenser is used in an arc furnace installation for power factor correction and for the suppression of the light flicker resulting from furnace operation. A condenser used for suppression of light flicker on an arc furnace load must have a low transient reactance. A normal 30,000-kva, ½-psig hydrogen cooled condenser has a transient reactance of approximately 48 percent, while application for light flicker suppression requires a reactance of about 30 percent.

It was determined that the completely supercharged generator could be applied as a condenser for this application at a nominal rating of 30,000 kvar, 3600 rpm, at 10-psig hydrogen pressure. On a 30,000-kva base it has a transient reactance of 20.6 percent making it ideally suited for arc furnace application. Because both the rotor and stator copper are cooled by direct contact with the cooling gas, the machine can be operated at increased kvar ratings at higher hydrogen pressures. For its present service, however, a rating of 30,000 kvar is adequate.

Since this is a two-pole machine, the usual reduced voltage starting scheme with a starting winding on the rotor is not practical and starting is accomplished by means of a wound rotor induction motor geared to the condenser shaft. The main exciter is also driven through the same gear.

At this time it seems probable that hydrogen pressures higher than the present standards will be used on synchronous condensers built in the future. However, to fully utilize the advantages of these higher pressures, direct cooling of the stator copper would be necessary, and this has so far been considered impractical on machines operating below 1800 RPM.

HYDROGEN COOLED synchronous condenser, rated 50,000 kva is designed for 30-psig operating pressure. **(FIGURE 1)**



STEEL MILL power factor correction and elimination of light flicker are provided by this 30,000-kva, 30-psig synchronous condenser. **(FIGURE 3)**

5Kv MOTOR CONTROL CENTER INTRODUCED

RATCHET DOOR HANDLE moves contactor to the disconnect position before cubicle can be opened. Contactor carriage can then be safely and easily rolled out of cubicle. (FIGURE 1)

by H. A. WRIGHT
Control Department
Allis-Chalmers Mfg. Co.

"Why box all this air?"
is the question that led to development of a completely new space saving concept for 2.5 and 5-kv motor control.

FOR OVER 20 YEARS the electrical industry has been supplying high voltage, high interrupting capacity controllers with current limiting power fuses. The early starters were quite large and required aisles both front and rear. Users, conscious of floor space asked, "Why box all this air?"

The answer at that time was that room was needed for the control components, high voltage spacing and segregation, and the maintenance man. With this in mind, the first developments were to reduce the size of the components. Since there was still too much air, the requirement for the rear aisle was eliminated by designing a front access starter. At the same time, the space needed for maintenance men was eliminated by making the high voltage contactor roll out for service. The new equipment seemed to be the answer except that the power leads had to be unbolted to roll out the contactor and there was little space for the maintenance man to do this work. The result was reduced floor space but increased maintenance costs.

The latest development problem to be solved was the reduction of this maintenance handling time. By making the high voltage contactor and fuses plug-in, the maintenance time required to unbolt the power and control leads could be eliminated. However, using available components, the enclosure would still be about 7½ feet high and 3 feet square on the base.

To reduce the overall controller volume, an entirely new high voltage air contactor rated 400 amperes, 5-kv with 50-mva interruption and 60-kv impulse capacity was designed to fit in an enclosure 45 inches high.

Control center construction used

The new contactor design culminated in a new space saving concept — the first 5-kv motor control center. Figure 1 shows the new control center construction. Two squirrel cage motor starters are arranged one over the other in a 90-inch enclosure requiring half the floor space of current models.

ISOLATING SHUTTER, positively driven, covers high voltage stationary portion of disconnects when contactor carriage is withdrawn from cubicle. Service dolly provides convenient means of handling upper carriage for inspection. (FIGURE 2)

Each 45-inch section houses a complete motor starter, consisting of the carriage, fixed portion and low voltage control.

The contactor carriage is rolled in and out of the primary disconnects plug by a helical drive mechanism. The racking mechanism is externally operated by a reversible ratchet handle providing practically effortless operation. The carriage arrangement shown in Figure 2 is obtained by using the new contactor mounted on wheels as the basic unit, to which is added the current limiting fuses, plug-in devices, and high voltage control panel. The insulation between phases and between the phase and ground is the new flame-retardant, arc track resistant glass polyester. Considerable reduction in the size of the carriage was made possible through the use of this new insulation.

The stationary line and load high voltage power primary disconnects and racking mechanism are located on the rear wall of the 45-inch high compartment as shown in Figure 2. An isolating shutter, positively driven both open and closed by the racking mechanism, covers the line terminals when the carriage is racked out.

The incoming power lines are connected to the top stationary primary disconnects on the rear wall. The corresponding load terminals are connected by cables to the current transformers which are mounted on the left-hand side-sheet. The user's outgoing load connections are made to the opposite side of the current transformers. The low voltage control power source and control connections are taken from the removable carriage to a plug-in terminal block, having 19 circuits. All inter-control connections from the carriage are made in the form of a trailing cable which extends to the user's control terminal block on the side-sheet. This permits the contactor to be rolled completely out of the cubicle for maintenance and adjustment without disconnecting the control power, provided of course, that a separate control power test source is used.

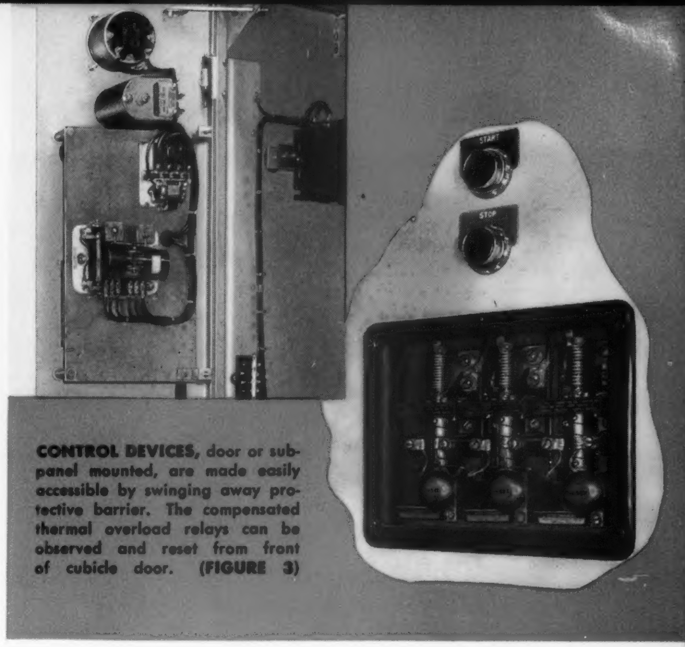
The control components are mounted on a sub-panel which in turn is mounted on the enclosure door. A hinged lift-off protective barrier is placed over the control panel. This barrier also serves as a shield for the user's control terminal block when the front door is closed. The barrier is shown in Figure 2. The overload relays are mounted directly behind the viewing window and may be externally reset following a tripping operation. While two overloads are standard, provisions are included for mounting and connecting a third phase overload relay when desired, as shown in Figure 3.

Since the basic full voltage starter is 45 inches high, it is being made available for low balcony or wall bracket mounting as shown in Figure 4. Used in this way, the controller requires no floor space.

When controllers are joined together in a line-up, they may be provided with a common three phase power bus. The bus placed in a duct located on top of the enclosure is closed in from the bottom and has a removable access plate on top. With this arrangement, the bus is completely isolated from the controller.

Safety emphasized

Mechanical interlocking is provided to prevent the carriage from being racked out while the contactor is closed and prevents the door from being opened until the contactor is in the racked out position. An isolating shutter driven by the racking mechanism covers the line terminals when the carriage is racked out.



CONTROL DEVICES, door or sub-panel mounted, are made easily accessible by swinging away protective barrier. The compensated thermal overload relays can be observed and reset from front of cubicle door. **(FIGURE 3)**

To prevent accidental power interruption at the low voltage supply plugs, an electrical interlock completely interrupts the continuity of the low voltage control power circuit before the main primary disconnects separate. This transformer interlock provides additional safety to the maintenance man by preventing any separate source of control power from feeding back thru the control transformer when the carriage is in the racked out position.

The new control center design for high voltage starters promises to bring new safety standards for high voltage control. Simplified handling procedures are now possible for the maintenance man and at the same time valuable floor space can be conserved. Floor space is becoming more important in modern process plants where controls are mounted in conjested areas and where purged rooms are needed to keep control equipment away from contaminated atmospheres.

COMPACT DESIGN has made possible wall bracket or low balcony mounting of single, high voltage, motor starters — freeing more valuable floor space to gain better plant utilization. **(FIGURE 4)**

50,000 KVA INTERRUPTING CAPACITY IN HALF THE SPACE

TWO MOTOR STARTERS can now be installed in space previously required by one. Removable carriage providing safety and convenience for handling and inspection is built around new 2 to 5-kv contactor.

by **L. A. BURTON**
Control Department
Allis-Chalmers Mfg. Co.

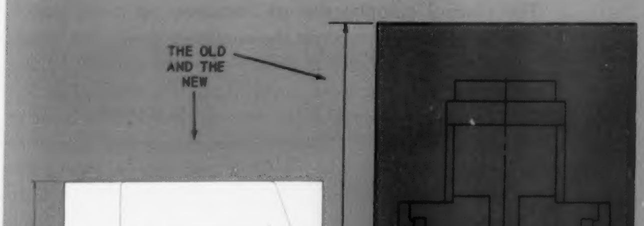
2 to 5-kv contactor was specially designed to fit in a new space saving motor control center type cubicle. Latest track-resistant insulation aided its designers.

IN THE TWENTY YEARS that the electrical industry has been supplying high voltage, high interrupting capacity motor controllers, many successful efforts have been made to reduce the size of these controllers and the floor space they occupied.

An analysis of the last generation of 2000 to 5000-volt motor control indicated that an effort to reduce the floor dimensions of the basic unit would not result in any significant reduction in size. A prospect of dramatic gains in space requirement, however, lay in reducing the height of the controller. Two complete units could be packaged, one above the other, in the same space required by existing controllers, thereby reducing floor area and building space requirements to one half. The prospect was sufficiently alluring to justify the complete redesign of most of the controller components. Since the heart of the controller is the high voltage contactor, a new contactor development program was initiated approximately three years ago to achieve the necessary size reduction.

The specifications for this development project were in essence to "produce a new 400-amp 2000 to 5000-volt contactor with an interrupting rating of 50,000 kva that could be packaged in one half the space required by existing designs."

It was apparent that the design effort would have two major divisions. The first was to design a contactor to fit the available space, and the second was to devise and perfect an interrupting mechanism that would function safely despite the confined space of the smaller cubicle.



COMPLETE REDESIGN of contactor made possible major space savings. Only double break inclined contact principle was retained. New arc chute and blowout arrangement plus new operating mechanism and magnet contributed most to the size reduction. **(FIGURE 1)**

To start with, it was established that a space 15 inches wide by 24 inches deep, by 28 inches high would be available. In contrast the existing contactor was 43 inches high. Preliminary layouts were made in a side view blocked in by the limiting dimensions shown in Figure 1. A compact arc chute was outlined together with a suitable double break contact structure. Experience with earlier designs has shown that double break contacts inclined at 45 degrees provided a number of advantages. A double break contact eliminates the potentially troublesome and short lived flexible leads. Contacts at 45 degree angle shed dirt, and utilize a wedging action to increase contact pressure. They also facilitate natural arc movement into the arc chute. This arrangement increases contact life by moving the arc off the contact surfaces faster. Although effective at all currents this natural arc movement is especially helpful at very low currents. Difficulty in interrupting these low currents has been experienced with other contact configurations.

The operating magnet was fitted into the layout in the remaining available space, below the blowout coils and adjacent to the contact structure, as shown in Figure 2. This location provided another desirable design feature, a clapper magnet. A pantograph drive was used to convert rotary motion of the magnet to linear motion of the movable contact structure. Rotary motion has rather obvious advantages. The relative motions of frictional surfaces are very small and these frictional forces occur at very small moment arms. Operating friction becomes a negligible factor even in dirty, dusty, and highly contaminated environments.

New insulation provides extra safety

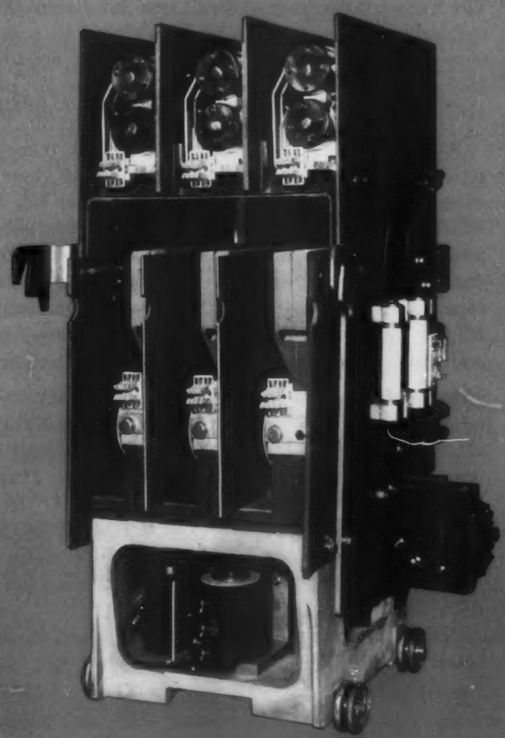
A new flame retardant, track resistant insulation material, was chosen for the contact support moldings. It is a glass reinforced polyester material containing a patented mechanism which increases the carbon tracking resistance by 100 to 200 times that of standard flame retardant polyesters and approximately 1000 times that of phenolic materials. This insulation was recently introduced in 15-kv switchgear to obtain extra protection against tracking.

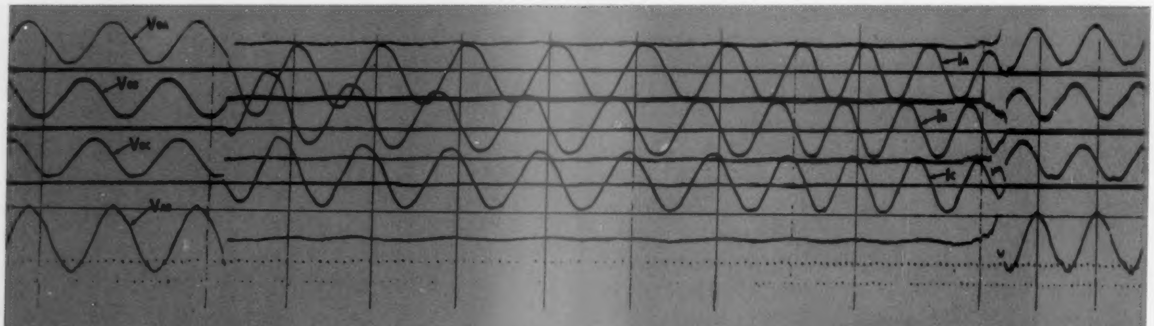
The contactor, is designed as an independent device for handling currents up to 400 amperes with an interrupting rating of 50,000 kva. It can be used, as a component in various control applications. When used in a space saving cubicle as a motor controller, it becomes part of a fused drawout assembly, as shown in Figure 3. Special moldings, of the same flame retardant, track resistant material used in the contactor were used to support the power fuses. In just 1½ inches of height, these moldings provide a barrier between the fuses and the arc chutes, mechanically mount the fuse clips, electrically isolate one phase from the next, and provide adequate creepage. Figure 3 illustrates how essential these moldings were in limiting the controller height to 45 inches.

A similar molding supports the power stabs on the back of the cubicle and makes the plug-in feature possible in a 32-inch cubicle depth.

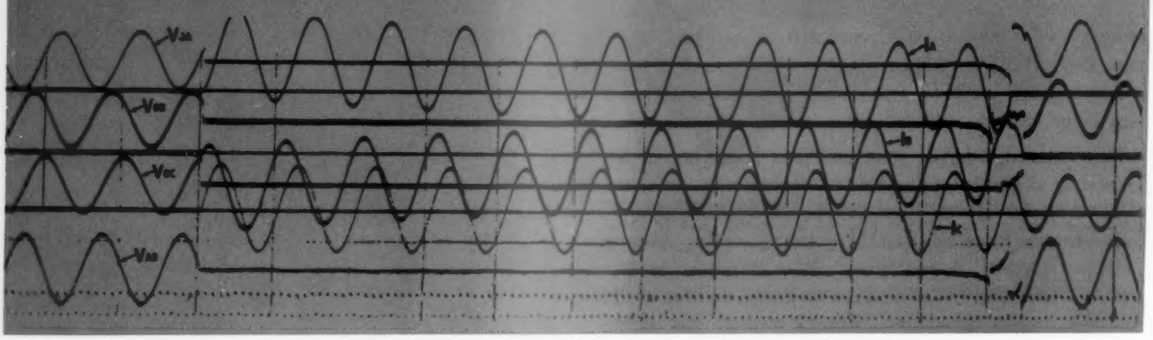
FLAME RETARDANT, track resistant moldings are used for contact support. Contactor barriers and arc chutes are readily removed and blowout devices hinged to give access to contacts. (FIGURE 2)

CARRIAGE DESIGN fully utilizes latest insulation materials to mount high voltage power fuses and disconnect contacts. Reduced height of contactor allows mounting space for fuses. (FIGURE 3)





CONTACTOR WITHOUT FUSES proved its ability to interrupt 50,000 kva at 2.5 kv (above) and 5 kv (below) for applications in which it is used as contactor or starter without short circuit protection. **(FIGURE 4)**



New arc chute design was required

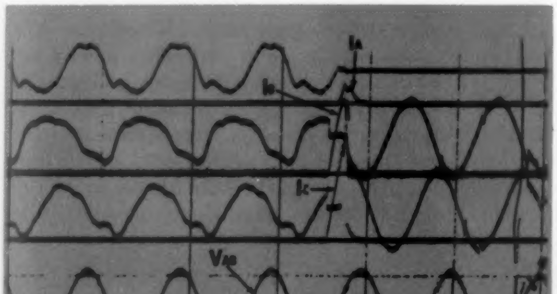
The second major area of work was the design of an interrupting mechanism. It was established by a series of tests that chutes of conventional design were not adequate, because the space for the dissipation of the arc products exhausting from the chutes had been drastically reduced.

A novel design was conceived and was refined by a series of tests on full scale segments of the chute at reduced ratings and with complete models, at full ratings. While it was possible to do some of this experimentation with the contactor alone, it was necessary to use prototype models of the complete controller to prove interruption capability. While the contactor was tested according to NEMA standards, many other tests were necessary to prove and improve its suitability for its various applications.

Interruption tests were made over the entire range of currents from a few amperes to 6000 and 12,000 symetrical amperes at 5000 and 2500 volts respectively. These tests were made at power factors less than 15 percent. Oscillograms of tests are shown in Figure 4. In a controller protected by current limiting fuses, the contactor will have to switch motor inrush currents which will always be less than 4000 amperes and usually much less than 3000 amperes. The test shown in Figure 5 was made to prove its through-current carrying ability during fault interruption of the maximum size power fuse used with the contactor.

Operational tests of pick-up, drop-out, bounce, and travel showed that the device picked up at 65 percent of rated voltage with negligible closing bounce. Since drop-out is less than 35 percent rated voltage, it is not sensitive to voltage dips. Heat runs were made at full ratings with both new and used tips, simulating realistic field conditions. The contactor was checked for corona at 5 kv, was subjected to a withstand test at 15 kv for one minute, and passed an impulse test of 60 kv between phases and from each phase to ground.

New design has brought not only new compact dimensions, but also added safety and convenience for the control of 2300 and 4160-volt motors. The reduction in size of the contactor itself will also aid those designing other equipment requiring contactors of this high rating.



THROUGH-CURRENT TEST was made with 60,000 asymmetrical amps which is maximum let-through current of current limiting fuses used with the contactor. **(FIGURE 5)**

NO-LOAD TAP CHANGERS / Use LTC Mechanism

TAP CHANGER is mounted integrally with core and coils. Construction for transformers is similar to regulator shown. **(FIGURE 1)**

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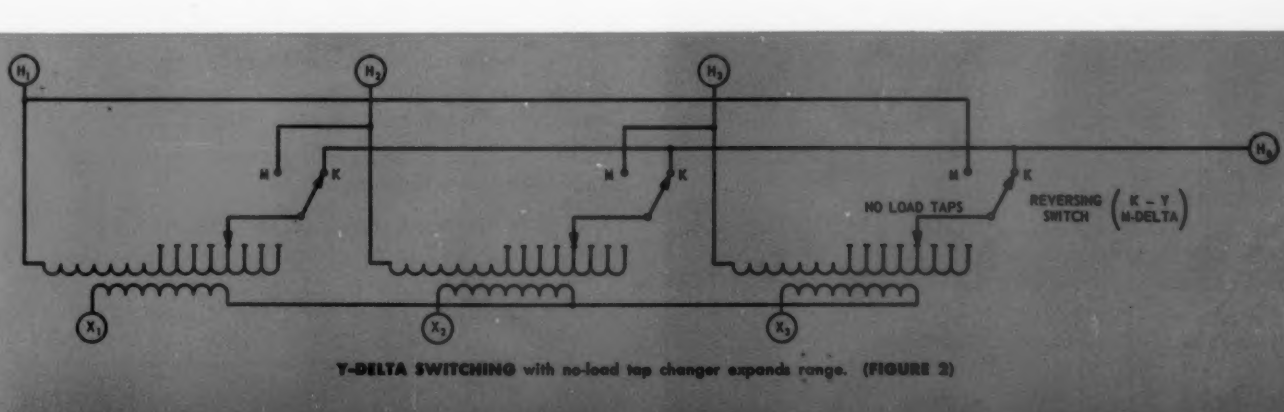
Standard load tap changer mechanisms are now being used for motor operated no-load tap changers. Lower cost and greater reliability are gained.

MANY TRANSFORMERS are provided with manually operated tap changers for de-energized operation or no-load tap changers as they are sometimes called. For many applications such as furnace transformers where taps must be changed frequently, motor operation of the tap changer is generally required.

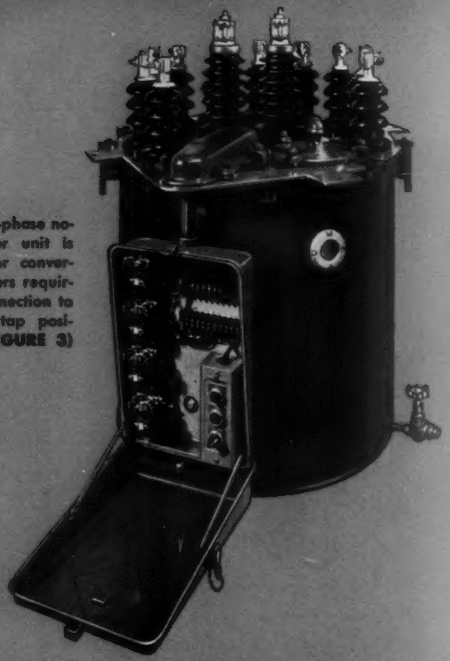
A new approach to motor operated tap changers for de-energized operation is to utilize standard load tap changing mechanisms and dial switches. A load tap changer has as one of its essential elements, a power driven dial switch. These motor driven LTC mechanisms are produced in quantities hundreds of times as large as the motor operated tap changers for de-energized operation. The advantages of quantity production and use are not only lower costs, but also increased reliability. Being produced in greater quantity there is greater uniformity because of the more extensive tooling for the standard mechanism. The LTC is a much more thoroughly tested device than the more special no-load tap changer can be.

In the past, motor operated LTC's have been constructed with a basic tap changer for de-energized operation designed for manual operation and adding, externally to the transformer, a motor operated gear reducer which drives the tap changer. The mechanism is stopped on position with dynamic braking or friction braking.

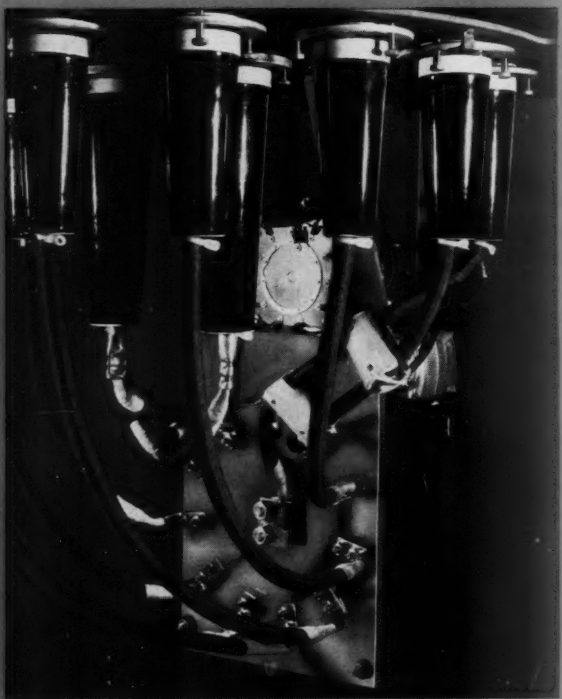
Evolution has taken place in the design of feeder voltage regulators employing load tap changing. For circuits of the 15-kv class and below, the modern regulator employs unit construction. The LTC is mounted directly on the core and coils of the transformer parts. This unit construction requires no oil tight studs between compartments and eliminates the necessity of an insulating panel with gaskets which must be oil tight. Continuous leads with no bolted connections except at the dial switch are



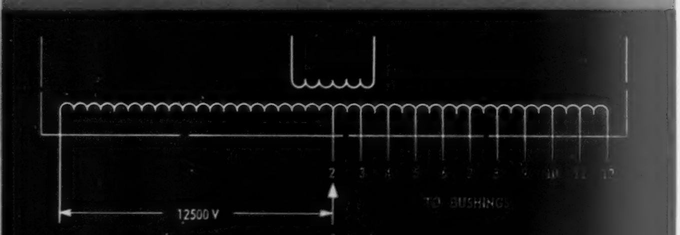
Y-DELTA SWITCHING with no-load tap changer expands range. **(FIGURE 2)**



SEPARATE single-phase no-load tap changer unit is now available for conversion of transformers requiring manual reconnection to motor operated tap position control. (FIGURE 3)



MOTOR OPERATED DIAL SWITCH, and control interlock switch of single-phase tap changer are operated under oil. (FIGURE 4)



ELEVEN TAP POSITIONS are provided with the single-phase no-load tap changer mechanism of Figures 3 and 4. (FIGURE 5)

possible with this unit construction. The assembly of the tap changer and the transformer is completed on the assembly floor where the complete assembly can be inspected before tanking.

A modern load tap changer motor is mounted under oil in the main transformer tank. The oil not only provides lubrication and cooling, but prevents moisture and dirt from reaching the motor. As a result, failures on these load tap changer motors are practically non-existent.

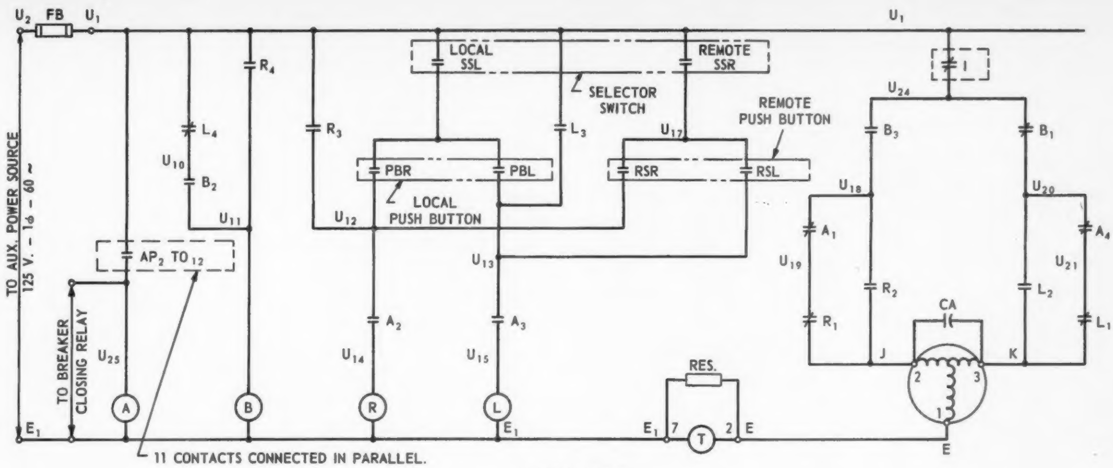
Accessories required with tap changers such as position indicators and control switches are made in quantities and thoroughly proved not only by experimental tests, but also by field service. Load tap changers which are positioned by a latch falling in a notch assure proper contact positioning. The operating requirements of motor operated no-load tap changers are the same as for load tap changers except generally no-load tap changers have fewer operations. The placement of the operating motor under oil was questioned at first for load tap changers, but over 20 years of service has proved its superiority. The advantage of accomplishing all alignment on an integral unit out of the tank eliminates misalignment troubles in the field. Change from the Y to delta connections can be made with the reversing switch of the dial switch. One particular application where this construction can be used to advantage is on furnace transformers. Normally these require Y-delta switching in addition to changing taps. A winding diagram incorporating Y-delta switching and a total of eight tap positions is shown in Figure 2. The only modification made to the standard dial switch was the replacement of sintered copper tungsten contacts with copper contacts because copper has lower resistance and the extra expense of the sintered copper tungsten serves no useful purpose in this case. Figure 1 shows a three-phase regulator with a load tap changer mounted on it. If the tap changer were used as a no-load tap changer the appearance of the tap changer would be the same.

These units may be either single or three phase. Separate motor operated tap changers can be supplied for existing transformers. A separate single-phase tap changer is shown in Figures 3 and 4. This arrangement provides a total of 11 tap positions for the transformer winding of Figure 5. Secondary voltages as shown by Table 1 are obtained by means of the remote controlled motor operated tap changer.

The simplest type of control is to provide a manually operated control switch to move the mechanism in either the raise or lower direction and to release the control switch when the desired position is reached.

Other features, however, are added for safe operation. These include the following:

1. A normally closed contact on the circuit breaker is placed in the no-load tap changer motor circuit. This



LEGEND

A .... On position auxiliary relay coil
A1 Thru 4..... On position auxiliary relay contacts
B .... Auxiliary control relay coil
B1 Thru 4..... Auxiliary control relay contacts
CA........... Capacitor
FB .... Fuse block (10 amp)
I............. Interlock switch on circuit breaker
L .... Lower auxiliary relay coil —
L1 Thru 4...... Lower auxiliary relay contacts
MM .... Mechanism motor
AP_2 Thru 12.... On position switch (closed on operating positions)
OP_2 Thru 12... On position switch contacts for on position lights (closed on operating positions)
PBL .... Lower push button switch
PBR .... Raise push button switch
R .... Raise auxiliary relay coil
R1 Thru 4..... Raise auxiliary relay contacts
RSL .... Remote lower push button switch
RSR .... Remote raise push button switch
SSL .... Local control selector switch
SSR .... Remote control selector switch
T .... Mech. motor trip relay coil

CONTROL CIRCUIT of single-phase 11-position tap changer unit has additional safety interlock connections to assure proper operational sequence. **(FIGURE 6)**

device keeps the tap changer from operating while load current is flowing.

2. A series of parallel contacts located on each of the tap changer operating positions, are placed in series with the power circuit breaker closing circuit. These interlocks prevent tap changer arcing and subsequent failure.

3. Position indication is at the point of control. This feature can be accomplished either by the use of a remote position indicator or by having individual lights connected to individual switches mounted on the tap changer.

For some designs, a circuit may be set up to seal in tap changer operation in an intermediate position to insure that each stopping position will be an operating position.

More complete automatic operation may be obtained by adding a control switch with preselected positions. Semi-automatic or automatic time sequence operation can be set up for a complete load cycle. A control circuit for operation of a single-phase 11 contact no-load tap changer with some of these features is shown in Figure 6.

Since load tap changers are now available to cover a wide range of voltages and currents as existing in commercial transformers, a suitable mechanism is now available for any application where a motor operated no-load tap changer is required.

TABLE I

INPUT	OUTPUT	
	Tap Position	Volts
12500 Volts	2	100%
	3	91
	4	83
	5	77
	6	71
	7	67
	8	63
	9	59
	10	55
	11	53
	12	50%

MODERN INSULATION and standardized construction techniques for 5-kv switchgear have greatly simplified assembly of long lineups. Molded flame-retardant glass-polyester insulation at bus joints keeps connections clean and eliminates need for taping or compounds. Should changes in the lineup become necessary with system growth, bus joint caps can be quickly removed. Bus runs for the lineup are completed on basic cubicle framework. In other compartments of finished gear, the new insulation is also used in either slab or molded form for inter-unit bus supports, secondary contact supports and stand-off insulators.

Allis-Chalmers Staff Photo by Clarence Hansen

A Second Look at SYNDUCTION MOTORS

SPECIAL ENCLOSURES, mounting provision and torque characteristics are required for most applications. Group of motors shown are part 448 being readied for cycling frequency drives.

by **WILHELM A. ANDERSEN**
Norwood Works
Allis-Chalmers Mfg. Co.

Actual "Synduction" motor applications are now requiring tailored designs made in volume for wide variety of drives.

THE "SYNDUCTION" MOTOR is a synchronous-reluctance type machine of improved design that, over the past four years since its development[1], has been applied to a large variety of drives.

They have been filling the gap between the limited reluctance type motors and the larger dc excited synchronous machines. They are also being used in a number of applications normally covered by induction motors.

Although these applications led to the development of a complete line of 60-cycle motors on the same frame size as the identically rated induction motors, "Synduction" motors conforming exactly to the user's load characteristics, mounting provisions and environmental conditions are becoming more predominant. By offering optimum performance and minimum size for a specific application, these motors have little competition. Many of these tailored designs have now received user's approval on the basis of extensive field tests. The torque characteristics of these machines requires special attention in each application.

High pull-in torque available

The pull-in torque of any synchronous motor may be defined as the maximum torque under which the motor will pull a connected load into synchronism. This torque must cover both the actual horsepower load during pull-in and the torque necessary to accelerate the load inertia. In many variable speed (variable input frequency) drives this inertia may be so large that the required pull-in torque is the determining factor in the motor design.

Numerous prototypes have been built for the synthetic fiber industry on the NEMA 50 frame series (7 inch yoke diameter). The pull-in torque of these motors was increased to the point where the motor will accelerate and pull into synchronism an external inertia 80 times greater than its own rotor inertia at speeds up to 6000 rpm. A ½ hp, 5000-rpm, 4-pole motor that must synchronize a load inertia of 2 lb-ft^2 at a friction load of 0.1 hp will have a rotor that is only 3¼ inches in diameter and 1¾ inches long.

This reduction in size is not only beneficial from the viewpoint of space consumption and cost, but will also keep the electrical losses and reactive power to a minimum. As a result, the power factor and efficiency at rated load will be superior to a motor requiring a larger rotor for synchronization of the same inertia. Furthermore, the pull-out torque of the small rotor will be greater than 200 percent of rated torque, thus insuring stable synchronous operation under heavy overloads.

In improving the pull-in capabilities of these motors, the design engineer must consider the harmonic content of the stator mmf wave, the rotor salient pole embrace, the minimum resistance of the squirrel cage, and the proper location of the rotor slots and magnetic barriers. Although engineers have yet to make a full and accurate quantitative analysis of these factors with regard to pull-in, limited analyses and experimental data have given sufficient information for design purposes.

In addition to the influence of the electrical design on the pull-in torque, the external variables have proved to

[1] The "Synduction" Motors, R. J. Dineen, 4th Quarter, 1956 Allis-Chalmers *Electrical Review.*

be important. The pull-in torque of a synchronous-reluctance motor is more affected by the power supply, load, and ambient conditions than internal characteristics. In any application where the pull-in could become critical, the following factors should be understood:

1. The pull-in torque is directly proportional to the square of the line voltage. Any decrease in terminal voltage, resulting from the high transient currents during pull-in or from other loads on the line, will result in a further decrease in pull-in torque.

2. The pull-in torque is inversely proportional to the power supply frequency stability and rigidity. A motor receiving its power from a large 60-cycle commercial power supply will often have a pull-in torque only half the value measured on a 10 to 50-kva variable frequency generator. This effect is primarily because the smaller power supply undergoes a minute transient frequency variation that will momentarily reduce the synchronous speed of the stator mmf wave to match more closely that of the rotor at the exact cycle of pulling into synchronism. Often as little as 0.01 percent is needed to be effective.

3. High harmonic content of the input voltage wave may adversely affect the motor's acceleration and pull-in torque. Since it would be both unreasonable and extremely difficult to analyze all power supplies, particularly those of variable frequency drives, the harmonic content should be minimized. Special caution should be given to input frequencies outside the 25 to 300-cycle range.

4. The motor can either pull in an unusually large inertia or a load approaching 150 percent of rated load, but not both without increasing motor size.

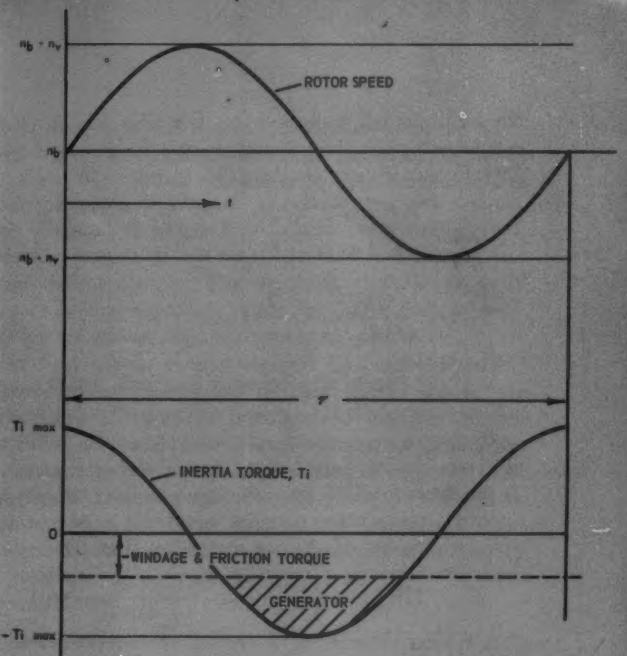
5. A "Synduction" motor requiring a Class B or H insulation system will have a lower pull-in torque than the corresponding Class A unit. Any substantial increase in motor temperature rise or ambient temperature will increase the rotor resistance and result in an increase of slip while the motor is running at subsynchronous speeds. The reluctance or synchronizing torques are less at the larger slip and a reduction of pull-in ability will result. In many instances, however, the rotor squirrel cage can be altered to compensate for the higher temperatures.

Motors applied to a "cycling" frequency

Fractional horsepower "Synduction" motors have also been designed to follow a sinusoidally changing frequency input while driving a specified inertia. The only relatively constant load would be the windage and friction of the external inertia load and the rotor assembly. In other words, the motor essentially supplies the power necessary to accelerate the inertia from the minimum frequency to the peak of the sine wave. On the downward slope the motor would theoretically behave as a generator.

Figure 1 shows the torque required to drive an inertia through a sinusoidal speed variation as a function of time. This inertia torque may be expressed as:

$$T_i = \frac{Wk^2}{g} \cdot \frac{dw}{dt} = \frac{Wk^2}{308} \cdot \frac{dn}{dt} = \frac{Wk^2}{308} \cdot wn_v \cos wt$$



CONSTANTLY CHANGING "Synduction" motor speed through sinusoidal pattern produces torque curve shown when driving given inertia. (FIGURE 1)

where $n = n_b + n_v \sin wt$.

n_b = basic speed of motor in rpm.

n_v = maximum speed variation in rpm.

w = speed variation cycle in radians per second $= \frac{2\pi}{\tau}$

Wk^2 = inertia of load and motor rotor in lbs-ft^2.

τ = time for one complete cycle in seconds.

The maximum inertia torque will be found for $\cos wt = 1$ or $wt = 0$:

$$T_{imax} = \frac{Wk^2\, w\, n_v}{308}$$

Taking into consideration the windage and friction losses and the reserve power needed for anticipated line voltage drops of 10 percent ($T \sim V^2$), the motor would be designed for a pull-out torque of:

$$T_{PO} > 1.21\,(T_{imax} + T_{\text{windage and friction}})$$

At $t = \tau/2$ in Figure 1, $T_i = -T_{imax}$ and the motor will generate power proportional to the difference between this torque and the constant windage and friction torques. When a large number of these units are being fed by one centralized variable frequency unit, the regeneration can be important in rating the alternator. However, the possibility of periodically feeding power back into the main 60-cycle supply is extremely small as the motor-generator and line losses are normally in excess of the motor output of

$$n(T_i - T_{\text{wind. and frict.}})/7.04 \text{ Watts}$$

To properly design the motor for optimum power factor, efficiency and temperature rise, the average inertia torque must also be determined.

$$T_{iavg} = \frac{4}{t_i}\int_0^{\tau/4} \frac{Wk^2}{308} w\, n_v \cos wt\, dt$$

or

$$T_{iavg} = \frac{4Wk^2}{308\tau} n_v = \frac{2}{\pi} T_{imax}$$

The windage and friction torques of the load alone are usually quite small in comparison to the inertia torques and can be neglected in most instances when calculating the average horsepower. Also, the additional load that they impose on the motor during acceleration of the Wk^2 is partly offset by the beneficial "drag" during deceleration. The average and rated horsepower of a cycling "Synduction" motor, therefore, would be:

$$HP = \frac{T_{iavg} n_b}{5250} = \frac{Wk^2\, n_v\, n_b\, 10^{-3}}{4.04\tau}$$

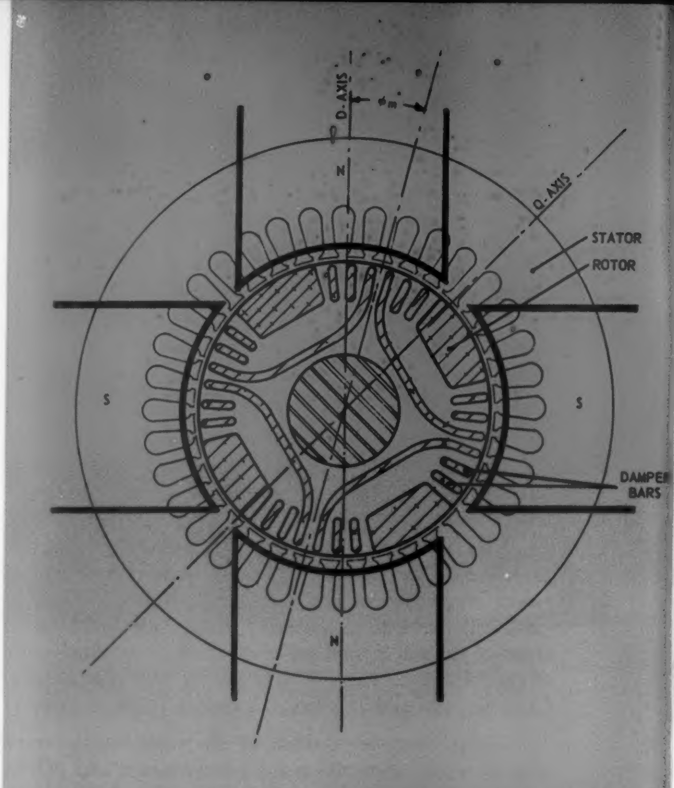
A typical example of a cycling application would be a motor driving an inertia of 1 lb-ft^2 at a base frequency of 100 cycles and undergoing a frequency variation of ± 3 percent in a period of two seconds. A four-pole motor would have a basic speed (n_b) of 3000 rpm and a speed variation (n_v) of 90 rpm. Using the equations, this motor must handle .918 lb-ft maximum inertia torque and be rated at .334 or 1/3 hp.

Hunting avoided

Unless special precautions are taken, a "Synduction" motor driving only a small inertia at load less than 10 percent of motor capacity may encounter instability problems at low input frequencies and the rotor would oscillate or hunt. These oscillations are characterized by cyclic variations in the rotor torque angle, ϕ_m, while the rotor is in synchronism with the stator field, and should not be confused with the transient oscillations which occur during pull-in at a varying slip. The displacement angle, ϕ_m, is shown in Figure 2.

The tendency to oscillate is present in all small synchronous-reluctance motors having a limited number of stator slots from which a crude magnetomotive force wave having a high harmonic content is induced. Normally, the loads and their inertias, the well-regulated power supplies, and the rotor damper bars will stabilize these motors and quickly dampen any oscillations that occur. If, however, the motor must operate far below its torque producing capacity with a small external inertia, the combination of stator harmonics and resonant frequencies between the motor and the power supply will occasionally result in a self-generated oscillation. However, these oscillations can be remedied if they do occur.

The cut-away view in Figure 2 shows a rotor surrounded by a 4-pole stator field and figuratively represented by salient poles. In reality this field would rotate at (30 × input frequency) rpm. The rotor is shown lagging the d-axis of the stator field by an angle, ϕ_m. This angle has two components; ϕ_{mo}, the torque angle needed for the constant load, and ϕ_{mv}, the variable angle of oscillation.



DISPLACEMENT ANGLE varies with load. Cyclic variations in this angle, or hunting, can occur at low motor input frequencies. **(FIGURE 2)**

Any relative movement of the rotor with relation to the stator field will (1) require an acceleration or deceleration of the shaft inertia, (2) induce currents proportional to the rate of flux change in the rotor bars, and (3) alter the reluctance torque. These three effects can be expressed by a balance of torques that eventually must offset a pulsating load or self-generated oscillation.

1. Inertia Torque, $T_i = \frac{Wk^2}{g} \frac{d^2\phi_m}{dt^2}$
2. Damping Torque, $T_d = k_1 \frac{d\phi_{mv}}{dt}$
3. Reluctance Torque, $T_R = \frac{mV^2(X_d - X_q)}{2 X_d X_q} \sin 2\phi$

Where V = the phase voltage

X_d = direct-axis reactance

X_q = quadrature-axis reactance

ϕ = rotor lag in electrical degrees

$= \phi_m \times \frac{\text{no. of poles}}{2}$

m = number of phases

T_i is determined primarily by the load inertia and cannot be varied. Therefore, the design engineer must give special attention to the damping and reluctance torques.

The damping constant, k_1, can be determined from the developed torque equation for an induction motor operating at a small slip, s.

$$T_{dev} = \frac{7.04}{N_s} \; \frac{mV^2 r_2/s}{(r_1 + r_2/s)^2 + (X_1 + X_2)^2}$$

$$\text{where } s = \frac{N_s - N}{N_s} = \frac{2\pi}{60N_s} \text{ radians/second} = \frac{2\pi}{60N_s} \; \frac{d\phi_{mv}}{dt}$$

For any oscillation at synchronous speed, s will be extremely small and $r_2/s \gg r_1$, X_1, or X_2.

$$T_{dev} = \frac{7.04}{N_s} \; \frac{mV^2 \, 2\pi}{r_2 \, 60N_s} \; \frac{d\phi_{mv}}{dt} = k_1 \frac{d\phi_{mv}}{dt}$$

$$\text{or } k_1 = \frac{2.21 \; mV^2}{N_s^2 r_2} \text{ for 3-phase motors}$$

Where N_s = motor synchronous speed.

r_2 = equivalent rotor resistance per phase of the damper bars and end ring.

The expression for k_1 clearly indicates that in order to obtain maximum damping, the rotor must have a maximum damper bar area consistent with permissible flux densities in the rotor teeth.

Depending on the source of oscillation, the reluctance torque may be modified in one of two opposing directions. For an oscillation imposed by a pulsating load, an increase of $(X_d - X_q)$ must be accomplished by decreasing the salient pole embrace to reduce the amplitude of oscillations. For a self-generated oscillation, a reduction of $(X_d - X_q)$ should be made to the degree of still maintaining the minimum acceptable pull-out torque. Any increase of $(X_d - X_q)$ would only amplify the harmonic content of the magnetic field and thereby increase the oscillation. The resulting increase in pole embrace will also provide space for larger damper bars and further reduce the self-generated oscillation.

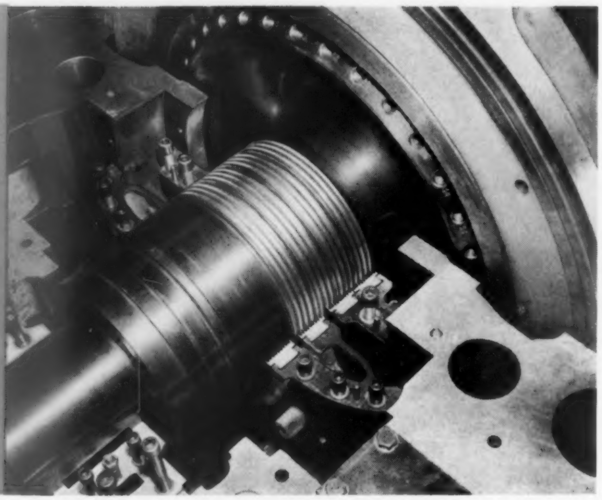
Applications demand special attention

Generally the "Synduction" motor developments are the result of specific needs for a brushless, rugged motor that will maintain a constant shaft speed independent of changes in load or input voltage. These needs exist primarily in applications where a wide range of nonstandardized loads and speeds prevail. In addition, mounting provisions and packaged units have often required a special frame.

Although standardization is undoubtedly the key to the success of induction and dc motors, it appears that the synchronous-reluctance motor is the exception. The foregoing analyses of custom-made "Synduction" motors should result in a better understanding of its limitations and potential, thus furthering the motor's proper application.

CORE BLOCKS for large accelerator project of Harvard University and Massachusetts Institute of Technology were assembled into a giant ring type choke capable of storing and releasing 600,000 joules of energy. The accelerator will be 240 feet in diameter and will develop six billion electron volts. Core blocks were assembled in bonded sections of grain-oriented transformer steel. Sections were then bonded under hydraulic pressure and bolted together. A completed block is shown on the right. Because of severe magnetic forces on the inductor core, extremely close tolerances are required on the 8000-lb blocks.

New Approach to LABYRINTH SEAL ANALYSIS



MATHEMATICAL APPROACH to combination labyrinth seals is now available using factors based on fluid mechanics. These seals are now being used effectively in higher rated steam turbines.

by GEZA VERMES
and
ROGER M. HAHN
Thermal Power Department
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Fluid mechanics provides more precise solutions to seal problems resulting in greater simplicity and increased seal efficiency.

MINIMIZING STEAM LEAKAGE through the shaft seals of a turbine has provoked studies and investigations for more than 50 years. Over this time, difficulties with shaft seal leakage have been aggravated by higher operating pressures and reduced space for shaft seals. Rapid turbine loading and unloading, causing a differential in expansion or contraction between the rotating and stationary turbine elements, have also added to the problem. Modifying purely thermodynamic formulas with fluid mechanics has substantially contributed to the accuracy of seal leakage calculations. Where thermodynamics is restricted to seals of simple configuration which occupy much axial shaft area, modification with fluid mechanics allows designers to cope with present-day seals with complex flow paths in confined space.

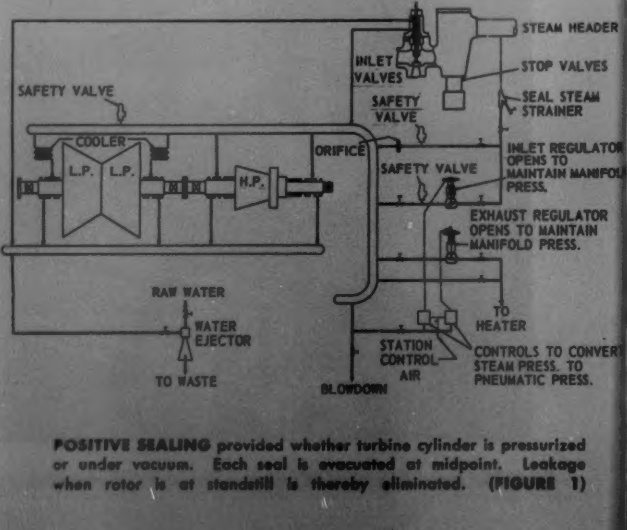
Steam is throttled

Labyrinth shaft seals are used to throttle steam flow past a rotating turbine shaft at the points where it emerges from the cylinder. The shaft seals also limit air inflow into the evacuated portion of the turbine at the condenser.

These functions are accomplished by providing a chamber midway through the steam path in each seal into which both steam and air flow. This steam and air is discharged through a water ejector or seal condenser, as shown in Figure 1.

A steam seal may be visualized as a series of constricting annuli along the end of the spindle. A pressure drop occurs across each. When sufficient constrictions are placed in the series, the seal is capable of limiting steam and air leakage to small values even though a large pressure differential exists across the cylinder wall of the turbine.

Martin's formula, shown in Figure 2a, which is a thermodynamic approach to seal leakage calculations, treats



POSITIVE SEALING provided whether turbine cylinder is pressurized or under vacuum. Each seal is evacuated at midpoint. Leakage when rotor is at standstill is thereby eliminated. (FIGURE 1)

$$W = 5.68\,K\,Ag\ \frac{p_0}{(RT_0)^{1/2}}\left[\frac{1-(p_N/p_0)^2}{N-\ln(p_N/p_0)}\right]^{1/2}$$

MARTIN'S FORMULA, previously used to evaluate seal efficiency, relied on thermodynamic principles. **(FIGURE 2a)**

$$W = 5.76\,K\frac{Ag}{(RT_0)^{1/2}}\ \frac{p_0}{(1-\alpha)^{1/2}}\left[\frac{1-(p_N/p_0)^2}{N-\ln(p_N/p_0)}\right]^{1/2}$$

THERMO-FLUID MECHANICS approach may be used to evaluate seal effectiveness for straight seals or may be modified for step or combination seal evaluation. **(FIGURE 2b)**

W = weight of fluid flow past seal per unit time
K = clearance factor of single regular annular orifice
Ag = geometrical flow area of a single annular orifice
p_0 = pressure existing before any seal strips are encountered
R = gas constant
T_0 = temperature of medium before any seal strips are encountered
p_N = pressure existing after N constrictions have been passed in fluid flow
α = residual energy factor

the operation of a seal as an isothermal process in which the temperatures of the fluid, gas or steam, entering and leaving the seal are practically the same.

Martin's formula also visualizes seals as operating on the principle of converting velocity energy issuing from an annular orifice to heat in the region between that constriction and the next downstream constriction.

Fluid mechanics changes concept

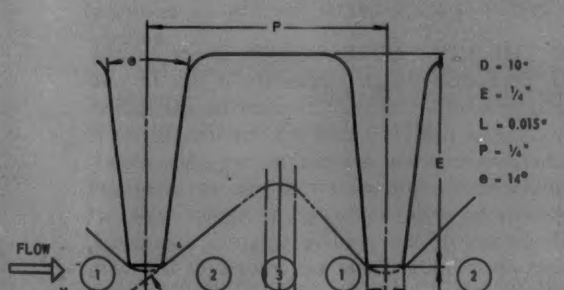
This mechanism of flow, however, exists in its pure form in few practical cases. Fluid mechanics provides a more accurate insight into the details of the flow picture. Martin's formula was therefore, modified to take into consideration the effects of the more complex flow patterns. The modified formula of Figure 2b provides added flexibility to evaluate losses in seals of modern turbines.

The complete conversion of velocity energy to heat in the area between constrictions implies that there is sufficient space between the adjacent constrictions for the fluid velocity to be completely dissipated. Only when the fluid velocity approaches zero between adjacent constrictions is the energy conversion complete and, under this ideal circumstance, the seal is performing its function most efficiently. A straight seal, such as shown in Figure 3, with seal constrictions widely spaced, approaches the ideal situation of complete velocity energy depletion.

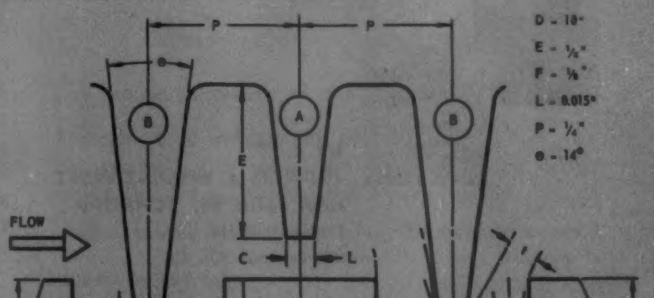
Fluid mechanics provides the key to evaluate precisely the residual energy which remains after the throttling cycle occurs through successive seal constrictions. This is the significant difference between the thermodynamic and the combined thermo-fluid mechanics approach since the thermodynamic approach is inadequate for evaluating the quantity of energy remaining after throttling. Actually, the spacing of seal strips and the proportioning of seal dimensions is the most important factor in determining the amount of residual energy remaining at the inlet to a following constriction. Fluid mechanics takes advantage of seal geometry in evaluating seal losses. Generally speaking, the smaller the distance between seal constrictions and the larger the clearance between the seal strip and the rotating shaft, the more residual velocity energy passes through the deceleration zone. An example would be a straight or step seal confined to a minimum axial space, as in Figure 4.

Differential in expansion effects seal performance

Seal configurations change appreciably during periods of load change, particularly when the seal is adjacent to a high temperature region of the turbine. At these times when turbine elements are being rapidly heated or cooled, the spindle, which is of relatively small mass, changes length faster than the cylinder which houses it. This inequality in axial growth rate is termed differential expansion. During such time, the seal is said to be operating under "off design" conditions. If a step seal were used for "off design" operation, seal projections would miss their intended lands, thus opening large gaps within the seal and reducing its effectiveness. In addition, the long



STRAIGHT SEALS can be used effectively with either high or low pressure ratios if adequate shaft length is available. **(FIGURE 3)**



STEPPED SEALS provide tortuous leakage-steam flow path with minimum of turbine shaft length required for seals. **(FIGURE 4)**

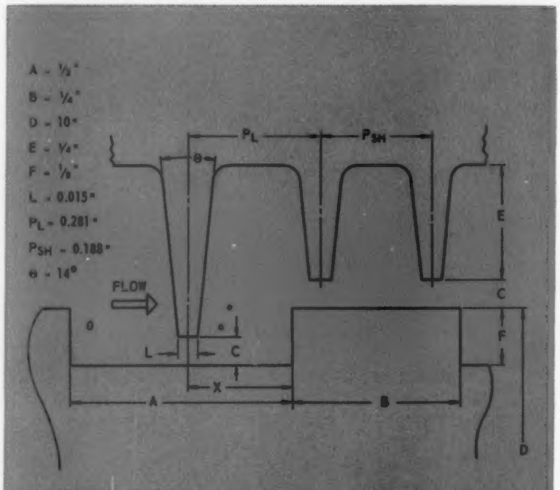
sealing points could rub on the lands, which are raised portions of the shaft, and destroy the seal. For these reasons, straight seals are often used where differential expansion is large.

Leakage controlled during differential expansion

Controlling steam leakage during periods of differential expansion has prompted the evolution of the combination seal, shown in Figure 5. During thermally unstable operating periods, some seal strips do not ride in close proximity to mating lands as they should. The effect is an axial displacement of seal strip with respect to the land. Therefore, the length of the groove adjacent to the longer seal strip must be the width of the seal strip tip plus a multiple of the magnitude of greatest differential expansion which will be tolerated in the design. The latter is necessary because differential expansion may find the spindle growing or contracting faster than the cylinder. Similar proportioning must also be made in the two small seal strips sandwiched between the longer seal strips so that at least one seal strip will ride close to the land during periods of differential expansion. This is critical in eliminating large internal gaps which nullify a seal's effectiveness in restraining steam leakage.

Fluid mechanics proves vital in establishing rates of steam leakage taking into account normal and "off design" seal geometry with "off design's" relative effect on sealing.

The derivation of the formulas are practically independent from the properties of the medium flowing through the seal. The fluid mechanics approach to seal calculations is therefore applicable to steam turbines as well as to gas turbines and such other rotating machinery as compressors and blowers.



COMBINATION SEALS are used where considerable differential in expansion between cylinder and spindle is encountered. (FIG. 5)

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1. "Labyrinth Packings," H. M. Martin, *Engineering*, January 10, 1908.
2. "A Fluid Mechanics Approach to the Labyrinth Seal Leakage Problem," Geza Vermes, ASME paper 60 GTP-12.

Determining Transformer ZERO SEQUENCE IMPEDANCE

by
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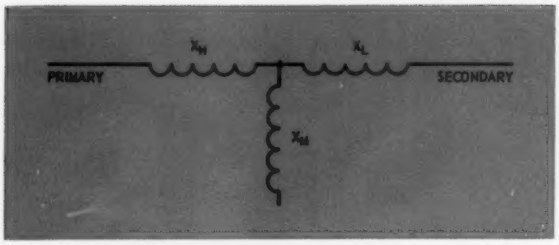
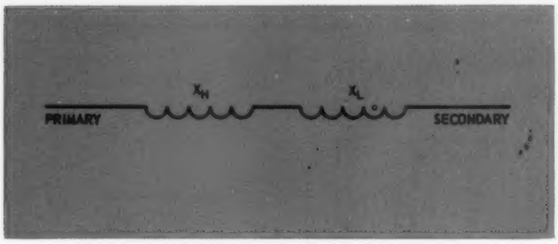
Zero sequence impedance of transformers, an important factor in proper system operation, can be readily calculated. Here are the basic considerations.

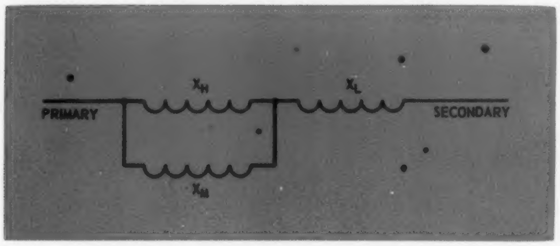
ONE OF THE MOST OVERLOOKED, if not entirely neglected transformer design parameters, is that of zero sequence impedance. Available literature on the subject generally concedes that for a shell type constructed transformer, the zero sequence and positive sequence impedances will be equal; whereas, for a core type construction, the zero sequence impedance is approximated as being 85 percent of the positive sequence impedance. The system operating engineer has learned to live with these limitations and, therefore, has not expected the transformer designer to engineer this parameter to his specifications. This understanding between designer and operating engineer is justified, as the zero sequence impedance cannot economically be altered with relation to the positive sequence impedance.



EQUIVALENT CIRCUIT of one transformer phase shows the impedance relation. **(FIGURE 1)**



EQUIVALENT CIRCUIT can be simplified by assuming exciting impedance high. **(FIGURE 2)**



PARALLEL COMBINATION is also given in an equivalent transformer circuit. **(FIGURE 3)**

A three-phase core-type, delta-Y transformer with the Y low voltage winding employing a helical construction and the delta high voltage winding having a disc coil construction is used as an example and all calculated quantities are referred to the primary or high voltage winding.

In calculating the positive sequence impedance, the transformer designer makes use of an equivalent circuit per phase which is shown in Figure 1.

When three-phase voltages are applied to the transformer the exciting impedance X_M is relatively high and for design purposes the equivalent circuit is reduced to that shown in Figure 2.

With all quantities referred to the primary side, there is no need to separate X_H and X_L and the designer calculates $(X_H + X_L)$ by means of the following formula

$$(X_H + X_L) = \frac{40.2\ T^2\ (MT)\ (3D + T_L + T_H)}{10^7\ Lx \left[1 + \dfrac{(D + T_H + T_L)}{2.8L}\right]} \text{ (At 60 cycles)}$$

The various quantities used in the formula are physical dimensions of the low voltage and high voltage coils and are defined in Appendix 1. If percent positive sequence impedance is desired, it is necessary to multiply $(X_H + X_L)$ by the rated kva phase current I and divide by the base phase voltage E as shown by the following relationship

$$\text{Percent IX} = \frac{(X_H + X_L)\ I \times 100}{E}$$

These formulas are well known to transformer designers and yield results which are quite accurate.

If this procedure is to be applied in the calculation of zero sequence impedance it is again necessary to refer to the equivalent circuit of Figure 1. In applying zero sequence or single phase voltages to the primary coils the exciting flux is forced out of each core leg into a path which is predominantly air or oil. For this reason it is suggested that the transformer can be roughly approximated as an air core transformer. This means that the exciting impedance X_M can no longer be neglected and takes on values which are comparable to X_H and X_L. X_M can be calculated using Brook's formula by considering the high voltage winding to be a simple air core reactor. X_M is then obtained as follows

$$X_M = \frac{0.366 \times 10^{-6} \left[\dfrac{(MT)_H (T)}{12}\right]^2 \left[F_1\right]\left[F_2\right] \times 377}{L + T_H + R}$$

The various quantities used in Brook's formula are defined in Appendix 2.

To arrive at an equivalent zero sequence impedance circuit it is assumed that X_H and X_L are equal and $X_H = X_L = \frac{1}{2}(X_H + X_L)$. An equivalent circuit with a parallel combination of X_M and X_H in series with X_L results as shown in Figure 3.

A study of the preceding circuit reveals that the inclusion of X_M reduces the impedance to a value less than $(X_H + X_L)$. The ratio of zero sequence impedance to positive sequence is evidently equal to

$$\frac{Z_0}{Z_1} = \frac{\dfrac{(X_M)\ (X_H)}{(X_M + X_H)} + X_L}{(X_H + X_L)}$$

A sample calculation of the ratio of $\frac{Z_0}{Z_1}$ is made in Appendix 3 to indicate the procedure to be followed. The particular example chosen was taken from design information for a 1000 kva transformer and the ratio of $\frac{Z_0}{Z_1}$ determined in this case is $\frac{9}{10}$. This result is easily verified by test data.

While the exciting impedance is the dominant factor relating zero and positive sequence impedances, it is by no means the only factor involved. It is very likely that the core side frames as well as the tank combine to produce, in effect, the action of a delta tertiary winding. The net result of this imaginary tertiary would place another

impedance in parallel with the circuit shown in Figure 3 and would again lower the impedance. However, due to the relatively large insulation clearances maintained to the side frame and tank, the impedance of this tertiary would be rather large. For this reason its effect is neglected.

Another factor to be considered is that of neutral lead reactance, which is of no importance in the determination of positive sequence impedance, but which must be contended with in arriving at the total zero sequence impedance. Considering the present day compactly designed lead assemblies, this factor may in many instances be regarded as negligible. In those cases where it is not insignificant, its effect is to counteract that of the exciting impedance. Thus, it is possible for the zero sequence impedance to equal or slightly exceed the positive sequence impedance depending on the relative magnitudes of neutral lead reactance and exciting impedance.

Consideration must also be given to the location of the exciting winding with respect to the core. If it is the winding nearest the core, the proximity of the iron will tend to keep the value of Z_0 up.

Thus, there are many factors which contribute to the zero sequence impedance, but basically it is the exciting requirement which tends to give a value which is less than the positive sequence impedance. Having designed for a given positive sequence, testing is then the proper way of determining Z_0. If, however, an approximation is necessary without test, the suggested method should be better than the 85% estimate. It should give values which are on the low side since the flux path is not as bad as this method assumes.

Table I shows a comparison of calculated ratios of Z_0 to Z_1, and actual test values. All tests were made by applying the test voltage to the winding located next to the core. The units having a ratio greater than one had the neutral lead remotely located with respect to the line leads, thus giving additional lead reactance.

REFERENCES

1. Zero Sequence Impedance, W. C. Sealey, *Allis-Chalmers Electrical Review*, June 1941.
2. *Relay Systems — Theory and Application*, I. T. Monseth and P. H. Robinson, McGraw-Hill Book Company, 1935.

TABLE I

Kva	Voltage	Ratio Z_0/Z_1 Calculated	Tested
2500	34400Δ-4160Y	.91	.99
3750	43800Δ-13090Y	.91	.98
5000	12000Δ-4160Y	.94	1.04
7500	13200Δ-4160Y	.93	1.03
7500	67000Δ-13090Y	.90	.97
7500	69000Δ-12470Y	.90	.96
10000	67000Δ-13800Y	.91	.98

APPENDIX 1

$(MT)_L =$ Mean turn of L.V. winding in inches

$(MT)_H =$ Mean turn of H.V. winding in inches

$$MT = \frac{(MT)_L + (MT)_H}{2}$$

$T =$ Turns per phase in disc winding (primary)

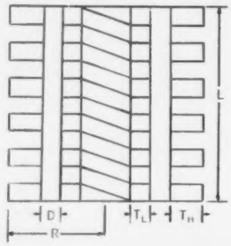
$L =$ Length of winding in inches

$D =$ Radial clearance high to low winding in inches

T_H and $T_L =$ Radial thicknesses respectively of the H.V. and L.V. windings

$R =$ Outer radius of H.V. winding

$X_H - X_L - X_M =$ Reactance in OHMS



Representation of cross-section of H.V. disc stack assembled over a helical L.V.

Fig. A

APPENDIX 2

Brook's Formula as applied to a disc H.V. winding

$$X_M = \frac{0.366 \times 10^{-6}\left[\frac{(MT)_H(T)}{12}\right]^2 \left[F_1\right]\left[F_2\right] \times 377 \text{ OHMS}}{L + T_H + R}$$

$$F_1 = \frac{10L + 12T_H + 2R}{10L + 10T_H + 1.4R}$$

$$F_2 = 0.5\,\text{LOG}_{10}\left[100 + \frac{14R}{2L + 3T_H}\right]$$

APPENDIX 3

Sample calculation of $\frac{Z_0}{Z_1}$ for a 1000 kva transformer with the following physical data:

$(MT)_L = 37\frac{3}{4}''$ $T_L = 0.96''$ $L = 20''$ $D = .78$

$(MT)_H = 51''$ $T_H = 1.65''$ $T = 889$ $R = 8.9''$

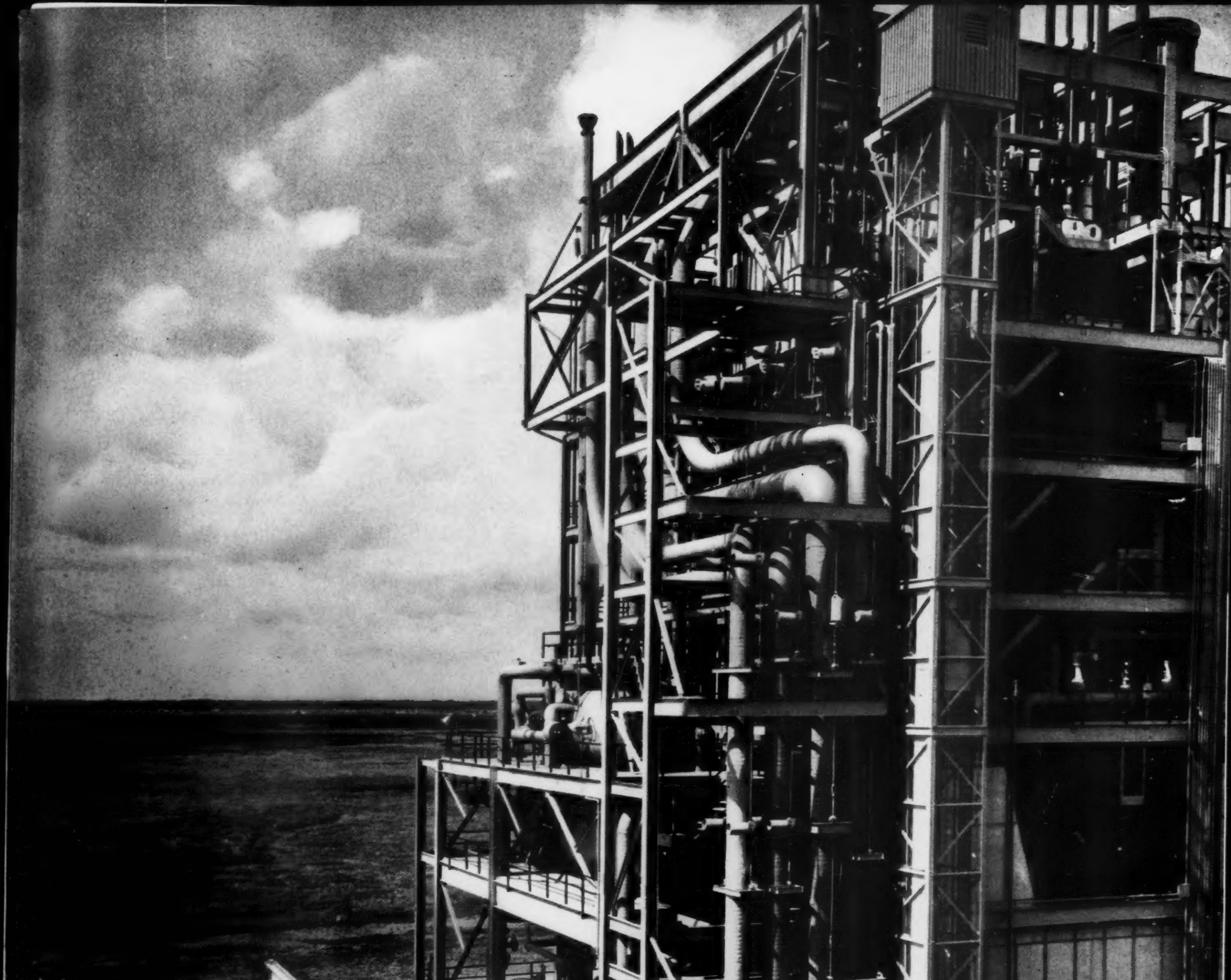
$$(X_H + X_L) = \frac{40.2 \times 889^2 \times 44\frac{3}{8} \times (2.34 + .96 + 1.65)}{10^7 \times 20 \times 1.06} = 33 \text{ OHMS}$$

$$F_1 = \frac{200 + 19.8 + 17.8}{200 + 16.5 + 12.5} = 1.04$$

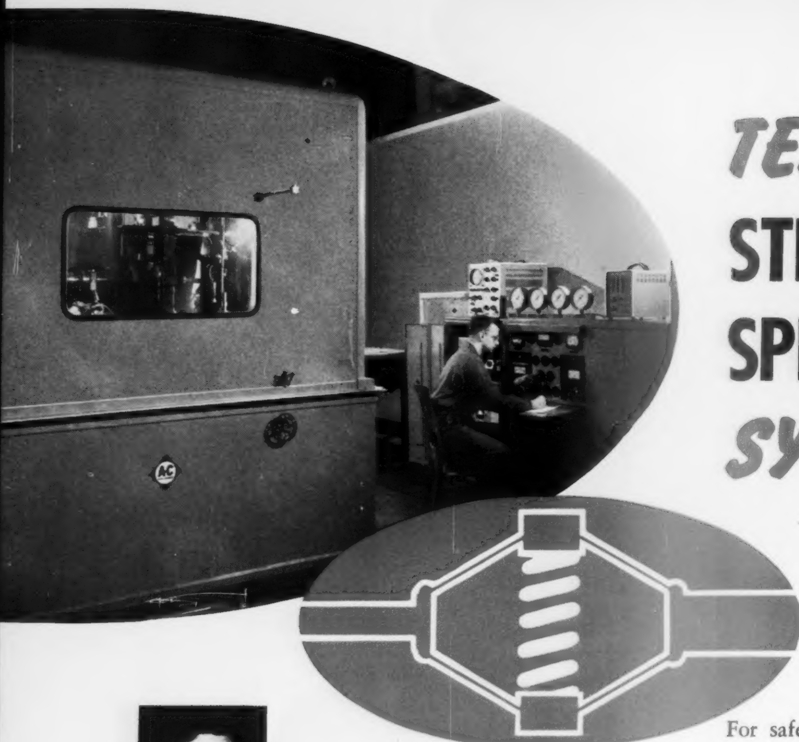
$$F_2 = 0.5\,\text{LOG}_{10}\ 100 + \frac{125}{40 + 4.95} = 1.00$$

$$X_M = \frac{0.366 \times 10^{-6}\left[\frac{(51)\ (889)}{12}\right]^2 \left[1.04\right]\left[1.00\right] \times 377}{20 + 1.65 + 8.9} = 66.5$$

$$\frac{Z_0}{Z_1} = \frac{\frac{(66.5)\ (16.5)}{(66.5 + 16.5)} + 16.5}{33} = 0.90$$



ENGINEERING BEAUTY at the T. H. Wharton station of Houston Lighting & Power Company — newest of utility's seven outdoor stations serving metropolitan Houston and its famous industrialized port area. The 220-mw triple-flow tandem steam turbine-generator unit combines many advantageous features of both turbine and generator design to account for its excellent performance record. Its unique turbine housing is pressurized and the walk-in housing over its 1500-kw dc exciter is entered through an "air lock" hurricane enclosure. The impressive 1.5 million lb/hr boiler and tiled turbine deck further typify this Ebasco designed station's modern construction.
Allis-Chalmers Staff Photo by Michael Durante



TESTING STEAM TURBINE SPEED-GOVERNING *SYSTEMS*

by EIVIND KINDINGSTAD
Thermal Power Department
Allis-Chalmers Mfg. Company

Extreme precision requirements of modern turbine speed-governing systems are met with new governor test facility.

THE BASIC FUNCTION of a steam turbine-generator unit speed-governing system is to maintain the balance between generator torque and turbine torque at a desired speed level.

If the load torque increases, the instantaneous torque unbalance causes the turbine and load to decelerate. The governor senses the speed drop and produces an error signal approximately proportional to the deviation from desired speed. The error signal is amplified sufficiently to change the flow of steam. Increased flow causes a greater torque to be developed by the turbine. Equilibrium occurs when turbine torque again matches generator torque and actual turbine speed is equal to desired speed within the regulation limits. Figures 1 and 2 show how this balance is normally achieved.

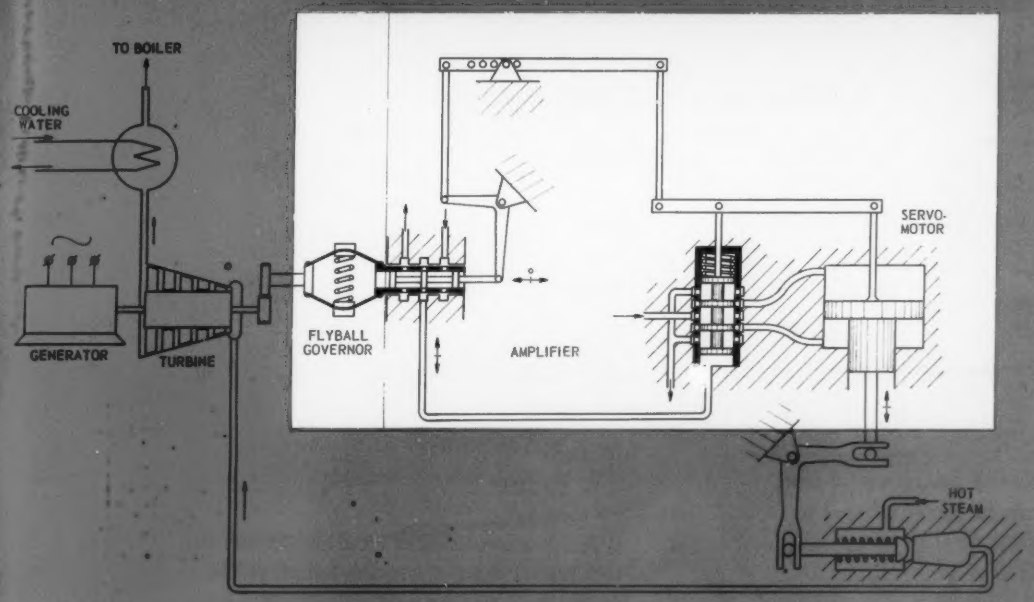
The effectiveness of the system is judged by how close the actual turbine speed maintains the desired speed. In the event of sudden loss of load torque, the entire instantaneous turbine torque tends to accelerate the rotating masses of the turbine and generator. If the torque-to-inertia ratio is high, the governor and amplifier must act very fast to close the valves before the unit reaches destructively high speeds. Unlike hydroelectric units, steam turbine-generators are not designed to take the centrifugal forces developed at maximum runaway speed. For safety, the turbine is usually equipped with a trip system that rapidly closes the steam inlet valves when the unit speed exceeds approximately 110% of normal running speed. But tripping the unit out is inconvenient in many plants and the speed governing system is normally designed to keep turbine speed below trip speed upon total loss of load.

If the generator is synchronized with a large interconnected system, a sudden increase in load will draw energy from the rotating inertia of the whole system. If the speed governor is too insensitive, other governors in the system may over-regulate and cause tie-line hunting.

The trend in today's steam turbines is towards greater capacities and higher energy steam conditions. The torque-to-inertia ratio of the rotors is increasing. This, together with the increasing demand for frequency accuracy (typical frequency deviations in the U.S.A. tie-line systems are a few hundredths of one percent), imposes severe stability problems on the speed-governing system.

Governor performance predicted

To predict the performance of a physical system, it is necessary to express the functions of each element by a mathematical model, that is to write the differential equations describing the input-output relations on the basis of elementary physical laws. Once the system of equations has been written, the performance of the system, such as transient response, frequency response and static accuracy may be predicted on the basis of manual or computer solutions of the differential equations. Unfortunately, any attempt to find the exact mathematical model of physical phenomena is bound to be fruitless because of the complexity of nature, or simply because the pertinent physical laws are not fully understood. However, by making certain approximations, the solution of the system of equations is usually accurate enough for engineering applications.



CLOSED-CIRCUIT hydraulic servosystem operates on signal of flyball governor, amplifies the signal and provides proper correction to steam flow. **(FIG. 1)**

To demonstrate this technique, let us consider the droop-governor of Figure 3. The speed sensor is a common flyball governor geared to the turbine shaft. An increase in speed throws the fly-weights outwards and the rotating pilot-valve sleeve, which is integral with the flyball governor, moves to the left to open the control-port to drain and thus reduce the pressure below the first stage servomotor. The resulting downward motion of the first stage servomotor-piston is fed back through the linkages A, B, C, D to reposition the pilot-valve spool in a direction to close the valve and restore the balance between hydraulic pressure-force and spring-force of the first stage servomotor. This servomotor has sufficient energy level to manipulate the 4-way valve controlling the position of the second stage servomotor through the lever E, F. The downward motion of the second stage valve opens the lower valve-port to high-pressure supply oil and at the same time the upper valve-port is opened to drain. The resulting pressure-difference across the piston moves the second stage servomotor upwards. This motion is fed back to close the valve and stop the servomotor motion since the valve-stiffness is negligible compared to the stiffness of the first stage servomotor. The second stage servomotor is powerful enough to move the main steam inlet valves.

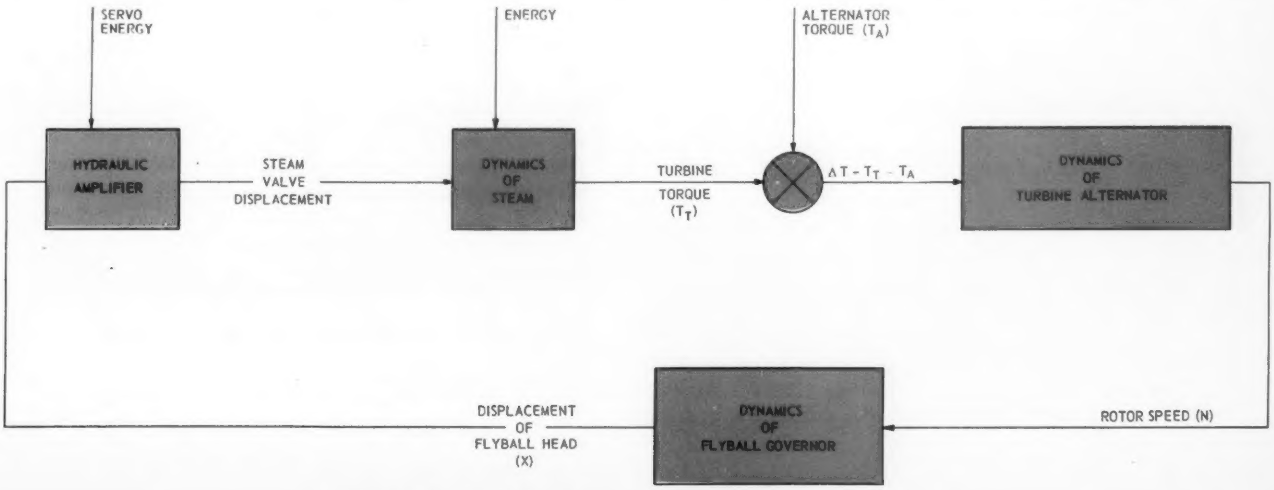
A simplified dynamic analysis of this system yields the following equations:

$$n = a \frac{d^2x}{dt^2} + bx \qquad \text{Eq. (1)}$$

$$x = T_1 \frac{dy}{dt} + y \qquad \text{Eq. (2)}$$

$$y = T_2 \frac{dz}{dt} + z \qquad \text{Eq. (3)}$$

where n = relative speed change
x = relative flyball head motion
y = relative position of first stage servomotor
z = relative position of second stage servomotor
a, b, T_1 and T_2 = constants



TURBINE TORQUE balances the generator torque requirements at a preset speed that is maintained by the governor system. **(FIGURE 2)**

Simplified Analysis of Speed Governor

A. *Flyball Governor*

The centrifugal force: $C' = \frac{G}{g} r N^2 \left(\frac{\pi}{30}\right)^2$ is balanced by a calibrated spring whose elongation is transmitted to the governor head (displacement X) through a trapezoid of leaf springs.

The relation between output (governor head position X) and input (governor speed N) is evaluated as follows:

Equation of motion of governor head:

$$m_g \frac{d^2X}{dt^2} = C - S$$

where m_g = mass of moving parts of governor reduced to governor head motion.

C = centrifugal forces referred to governor head motion.

$$= \frac{G}{g}\, r \left(\frac{\pi}{30}\right)^2 N^2 f(X)$$

$f(X)$ = transmission function.

S = resulting spring forces referred to governor head travel.

If a small but finite, perturbation from the static equilibrium point (X_1, N_1) is considered:

$$C = C_1 + \Delta C \ , \ S = S_1 + \Delta S$$

and $C - S = (C_1 + \Delta C) - (S_1 + \Delta S) = \Delta C - \Delta S$

since by definition $C_1 - S_1 = 0$

$$\Delta C = \frac{\partial C}{\partial N}\,\Delta N + \frac{\partial C}{\partial X}\,\Delta X$$

$$\Delta S = \frac{\partial S}{\partial X}\,\Delta X \cong \frac{dS}{dX}\,\Delta X$$

Thus: $$m_g \frac{d^2X}{dt^2} = \Delta C - \Delta S = \left(\frac{\partial C}{\partial N}\right)_{X = X_1 + \frac{\Delta X}{2}} \Delta N - \left(\frac{\partial S}{\partial X} - \frac{\partial C}{\partial X}\right)_{N = N_1 + \frac{\Delta N}{2}} \Delta X$$

$\frac{\partial C}{\partial N}$ = slope of curve shown in Figure 4.

$$C = \frac{G}{g}\, r_1 \left(\frac{\pi}{30}\right)^2 N^2 f(X_1)$$

Thus: $$\frac{\partial C}{\partial N} = \frac{G}{g}\, r_m \left(\frac{\pi}{30}\right)^2 2N\, f(X_m)\ ;$$

$$r_m = r_1 + \frac{\Delta r}{2};\ X_m = X_1 + \frac{\Delta X}{2}$$

Normally $\frac{\Delta N}{N_1}$ is very small,

so that $$\frac{\partial C}{\partial N} \cong \text{Constant} = \frac{G}{g} \cdot r_m \left(\frac{\pi}{30}\right)^2 2N_m\, f(X_m)$$

where $N_m = N_1 + \frac{\Delta N}{2}$

Thus: $$\frac{\partial C}{\partial N} = 2\frac{G}{g}\, r_m \left(\frac{\pi}{30}\right)^2 N^2_m\, f(X_m)\, \frac{1}{N_m} = 2C_m \frac{1}{N_m}$$

and $$\frac{\partial C}{\partial N}\,\Delta N = 2C_m \frac{\Delta N}{N_m} = 2C_m\, n$$

n = relative speed change $= \frac{\Delta N}{N_m}$

Figure 5 shows the curves $F = S(X)_{N = N_m}$ and $F = C(X)_{N = N_m}$ approximated by straight lines.

From the fig. $$\Delta F \cong \left(\frac{\partial S}{\partial X} - \frac{\partial C}{\partial X}\right)\Delta X = \frac{\Delta F_{max}}{X_{max}}\,\Delta X = \Delta F_{max}\, x$$

$$x = \frac{\Delta X}{X_{max}} = \text{relative governor travel.}$$

The equation of motion now becomes:

$$X_{max}\, m_g \frac{dx^2}{dt^2} = 2C_m\, n - \Delta F_{max}\, x$$

$$n = \frac{m_g X_{max}}{2C_m}\frac{d^2x}{dt} + \frac{\Delta F_{max}}{2C_m}\, x = a\frac{d^2x}{dt^2} + bx \quad \text{Eq. (1)}$$

This is a second order equation without damping (we have neglected valve-reaction forces) whose natural frequency is:

$$W_n = \sqrt{\frac{b}{a}} = \sqrt{\frac{\Delta F_{max}}{m_g X_{max}}} \left(\frac{\text{rad}}{\text{sec}}\right)$$

B. *Hydraulic Amplifier*

The governor head is integral with the input to the hydraulic amplifier, the rotating pilot-valve sleeve. For the sake of simplicity we shall assume that flow of oil through the valves is proportional to valve opening.

Then oil flow Q to the first stage servomotor is:

$$Q = A_1 \frac{dY}{dt} = F_1(\Delta X - M_1\,\Delta Y)$$

A_1 = area of first stage servomotor

Y = displacement of first stage servomotor

F_1 = pilot valve flow coefficient $= \frac{\partial Q}{\partial X}$

M_1 = feedback ratio $= \frac{AC}{BD}$

A, B, C and D are shown in Figure 2

Thus: $$\Delta X = \frac{A_1}{F_1}\,\frac{dY}{dt} + M_1\,\Delta Y$$

or since in the steady state $M_1 Y_{max} = X_{max}$

$$x = \frac{\Delta X}{X_{max}} = \frac{Y_{max}\,\frac{A_1}{F_1}\,\frac{dY}{dt} + M_1\,\Delta Y}{M_1\,Y_{max}}\ ;\ y = \frac{\Delta Y}{Y_{max}}$$

$$x = T_1\frac{dy}{dt} + y \qquad \text{Eq. (2)}$$

The time constant: $T_1 = \frac{A_1}{M_1 F_1}$ (sec)

Proceeding similarly the simplified equation describing the response of the second stage is: (See Fig. 2).

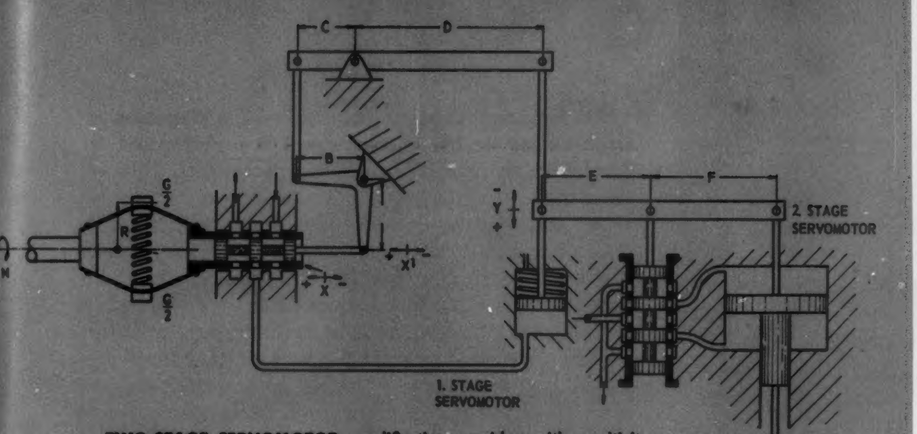
$$y = T_2 \frac{dz}{dt} + z \qquad \text{Eq. (3)}$$

where the time constant is $T_2 = \frac{A_2}{M_2 F_2}$ (sec)

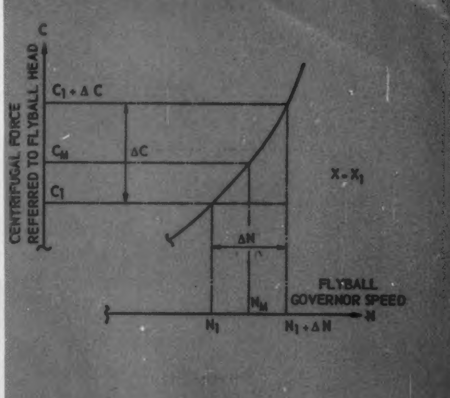
A_2 = effective area of second stage servomotor

M_2 = feedback ratio of the second loop $= \frac{E}{E+F}$

F_2 = flow coefficient of second stage valve



TWO-STAGE SERVOMOTOR amplification provides, with sensitivity, the necessary force to move the turbine inlet valves. (FIGURE 3)



CURVE gives relation between changes in governor head force and change in governor speed. (FIGURE 4)

The solution of Eq. (1), (2) and (3) provides a reasonably good approximation of the time response of servomotor motion to speed changes n for small perturbations and low frequencies.

As may be seen from Eq. (1), the equation of motion of the flyball governor is without a damping term. The lack of damping omission is a serious drawback for any element within a control-loop unless its natural frequency is an order of magnitude higher than the useful frequency band of the subsequent elements. Eq. (2) and (3) represent two exponential lags and are dynamically equivalent to a filter, as long as $W_n >> \frac{1}{T_1} > \frac{1}{T_2}$ so that the natural frequency of the flyball governor is attenuated sufficiently to give smooth servomotor response.

The trend towards higher accuracy and speed of response in modern designs means that the time constants of the hydraulic amplifiers are decreasing. Increasing the natural frequency of the flyball governor, on the other hand, means in general that the energy of the control signal available at flyball head is decreasing so that the hydraulic amplifier gain must be increased. Besides, stability problems arise as a result of additional lags in the amplifier loops because of such factors as compressibility of the oil (air contamination), piping, lags and flexibility of feedback-levers. Finally, the equations (1), (2) and (3) are in reality coupled because of the hydraulic valve forces reacting on the input elements.

Thus the problem of finding the combination of parameters that yields optimum response is quite complex. A tremendous help towards achieving this goal is the modern electronic analog computer. But the computer can only solve the equations it is given, so that it becomes imperative to check whether the differential equations written for the system are really representative. Once this is done, optimization of the system may be carried out with the help of a computer simulation much faster and with less expense than is possible working with actual system hardware.

As already pointed out, the validity of the theory rests upon the initial assumptions. For instance, in the case of Eq. (1) we have assumed the partial derivatives $\frac{\partial C}{\partial N}$, $\frac{\partial C}{\partial X}$ and $\frac{\partial S}{\partial X}$ constant in the region of normal operation of the flyball governor. To check this, and also to provide the actual numerical values, it is necessary to plot the curves of Figures 4 and 5 from experimental values.

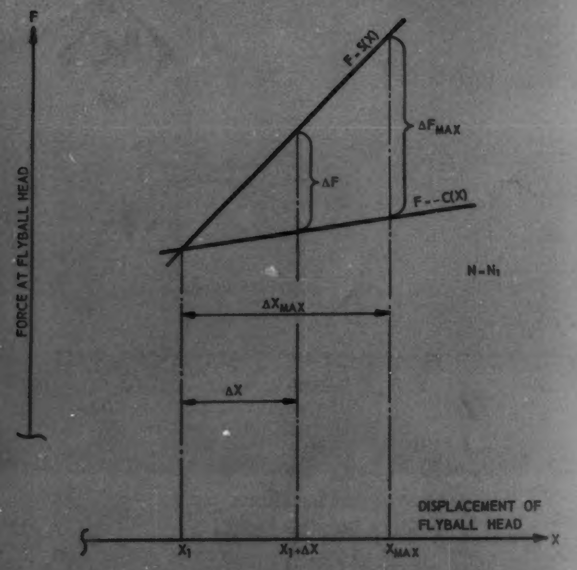
In arriving at Eq. (1) dynamic damping forces were neglected and non-linearities like dry-friction and backlash are not considered. These effects, if predominant, may change the mathematical form of Eq. (1). To establish its validity, therefore, frequency response and transient response tests are performed. The results of these dynamic tests yield numerical values of the parameters as well as a check on whether the mathematical form of Eq. (1) is representative or not.

Governor test stand requires extreme precision

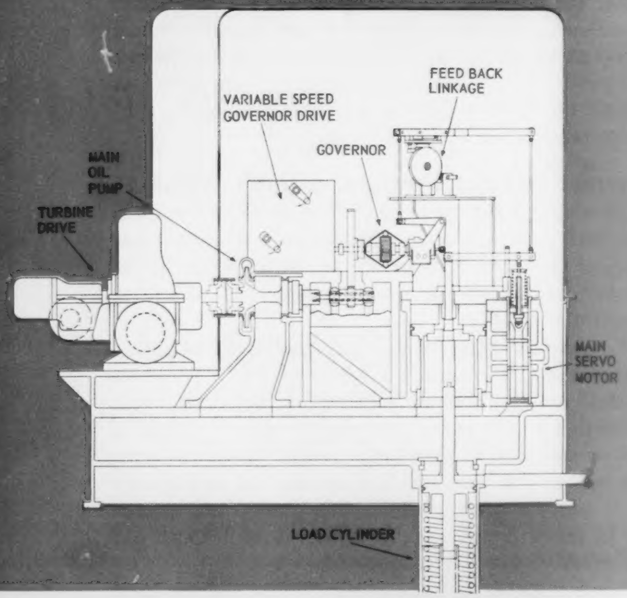
To provide this experimental verification of system equations and their parameters, the governor test stand was designed and built. This facility permits complete testing of steam turbine speed-governing systems under simulated operating conditions.

A major advantage of the test stand is its flexibility. Hydraulic and mechanical components can be tested individually or as part of the entire speed governing system. Production governors can be checked or special conditions, such as supercritical pressure or nuclear powered operation, can be simulated for experimental and development work. Conventional hydro-mechanical governing systems can be tested as well as other types . . . all with an ease and reliability heretofore impossible.

Figure 6 shows the basic functioning of the test stand. The oil to power the hydraulic amplifiers is supplied by a centrifugal pump driven by a small steam turbine. The pump has a relatively flat characteristic and has, to some extent, the advantages of a storage vessel. Oil pressure may be regulated with turbine speed or bypass valves. Temperature control of oil facilitates operation at different stable temperatures to include effect of viscosity. The flyball governor may be geared to the turbine-pump shaft, or driven from a separate input drive system. To facilitate measurement of system sensitivity and hysteresis, a stabilized frequency drive system is provided. A crystal oscillator, having a stability of one part in 10^6 per hour, generates a high frequency voltage that is stepped down



APPROXIMATED CURVES show force at the flyball head with relation to flyball head displacement. Nonlinear forces were not considered in setting up the motion equation. (FIGURE 5)



TEST STAND simulates actual turbine-generator speed-governor system. Displacement, flow, force and speed are readily measured. Recordings of transients of variables are made with latest instrumentation. (FIGURE 6)

through a binary frequency dividing circuit to desired drive frequency. The output is filtered, converted to three phase and amplified to power the synchronous drive-motor. A switch changing the frequency step-down ratio provides 21 frequencies in the range $N_0 \pm 0.005$ percent to $N_0 \pm 0.1$ percent. Greater frequency range is provided by feeding the filter circuit from a standard variable frequency oscillator.

For frequency response measurements, a sine-wave of amplitude up to 1% of rated speed is superimposed on rated speed by frequency modulating techniques. Due to the limited speed of response of the motor, this scheme covers the frequency band of modulated signal up to 5 cps. This is not sufficient, however, since the flyball governor resonance is typically 10-15 cps. To cover the frequencies up to 40 cps, a mechanical sine-wave-generator was provided. This consists of a synchronous motor driving one input to a differential gear through a heavy flywheel. The other input is driven by a scotch-yoke, the excenter of which is powered from an induction motor through a "Graham" variable speed transmission. The amplitude and frequency of the sine-wave thus superimposed on the output steady state speed may be varied by changing the speed reduction through the "Graham" drive and varying the throw of the scotch-yoke.

The test stand is fully instrumented to indicate and record all variables of interest; i. e., displacement, pressure, flow, force and speed. For example:

a. Speed of the flyball governor may be measured independently by a biased d.c. tachometer and a digital counter.
b. Differential transformer-type pickups are used for displacement measurements. The output is amplified and demodulated before it is fed to the indicator or recorder galvonometer.
c. Pressures are measured by utilizing strain-gauges in a bridge circuit mounted on miniature membranes. Bridge unbalance is proportional to pressure change.
d. Turbine-type pickups are used to measure flow of oil. A variable reluctance pickup triggers the input circuit to a digital counter each time an impeller blade of the pickup turbine passes. A converter provides an analog signal for recording.
e. Forces are measured by using differential transformer displacement transducers sensing the deflection of a calibrated steel ring.

A 14-channel photo-electric recorder provides convenient recording of transients of all variables.

Simulates overall system conditions

An analysis of the entire speed-control system must, of course, include the dynamics of the turbine-generator and its load. Due to the high cost and inconvenience involved in interfering with the normal service of today's large turbine-generators, straight forward test techniques, like frequency response and transient response to step-input, are seldom tolerable. If, however, the various input and output variables are recorded simultaneously under normal operation in the power station, it is possible by statistical techniques to evaluate the auto- and cross-correlation functions of input and output of elements and thereby calculate the transfer functions by well known relationships. With the transfer function in hand, the governor test stand facilitates closed loop tests of a proposed speed-control system by simulating the turbine-generator on an analog model either by making use of the drive turbine for the oil pump or the electrical drive system for the flyball governor.

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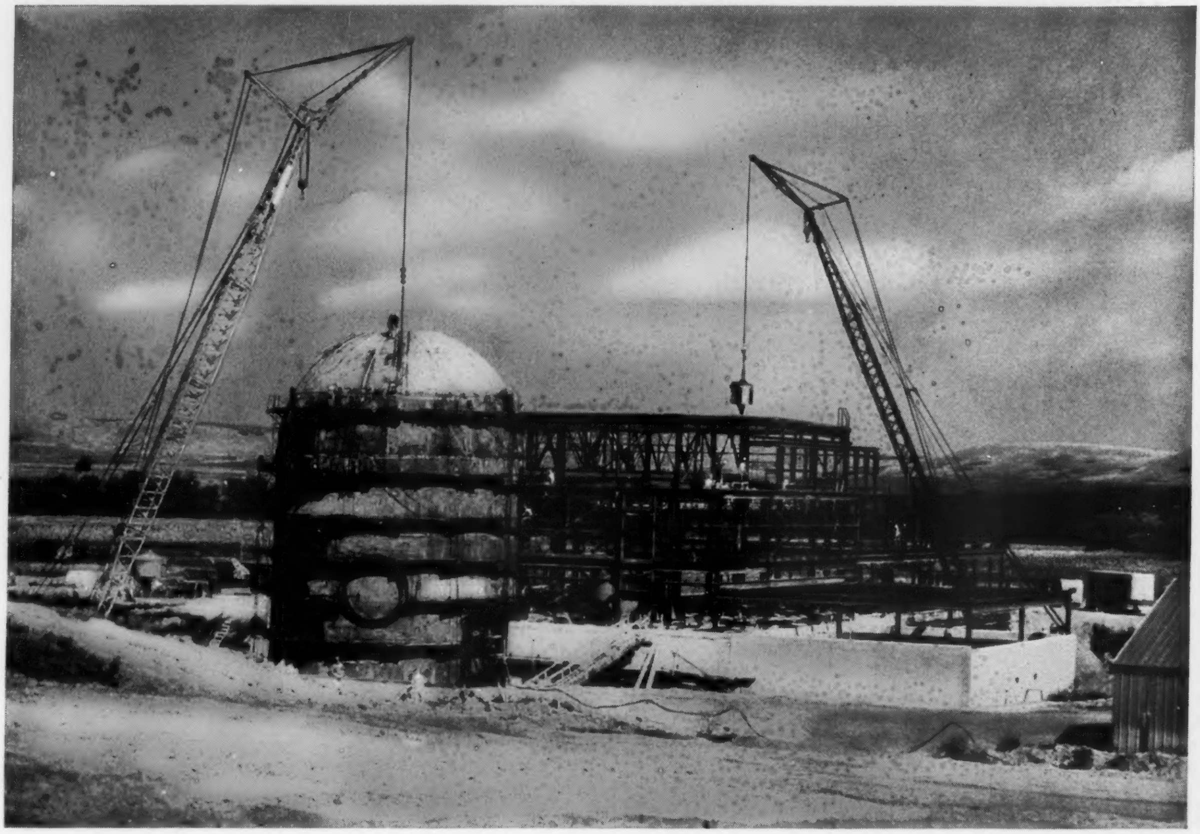


A status report:

nuclear superheat...on schedule

"Pathfinder" is the country's first nuclear superheat power station under construction. It is being built by Allis-Chalmers for Northern States Power Company. Cooperating in research and development are nine other midwest utilities* and the Atomic Energy Commission.

The above photo shows the containment, turbine, water treatment and administration buildings. Construction is on schedule.

The integral nuclear superheater will provide steam at 600 psig, 825 F. Other important design characteristics include a high power density — 46 kw per liter — and controlled recirculation — 60,000 gpm.

Only Allis-Chalmers has experience in both direct- and indirect-cycle boiling water reactors, fast breeder, gas-cooled and process-steam reactors and fusion. In addition, A-C has long been a leader in such steam plant equipment as turbine-generators, condensers, pumps, valves and water conditioning systems.

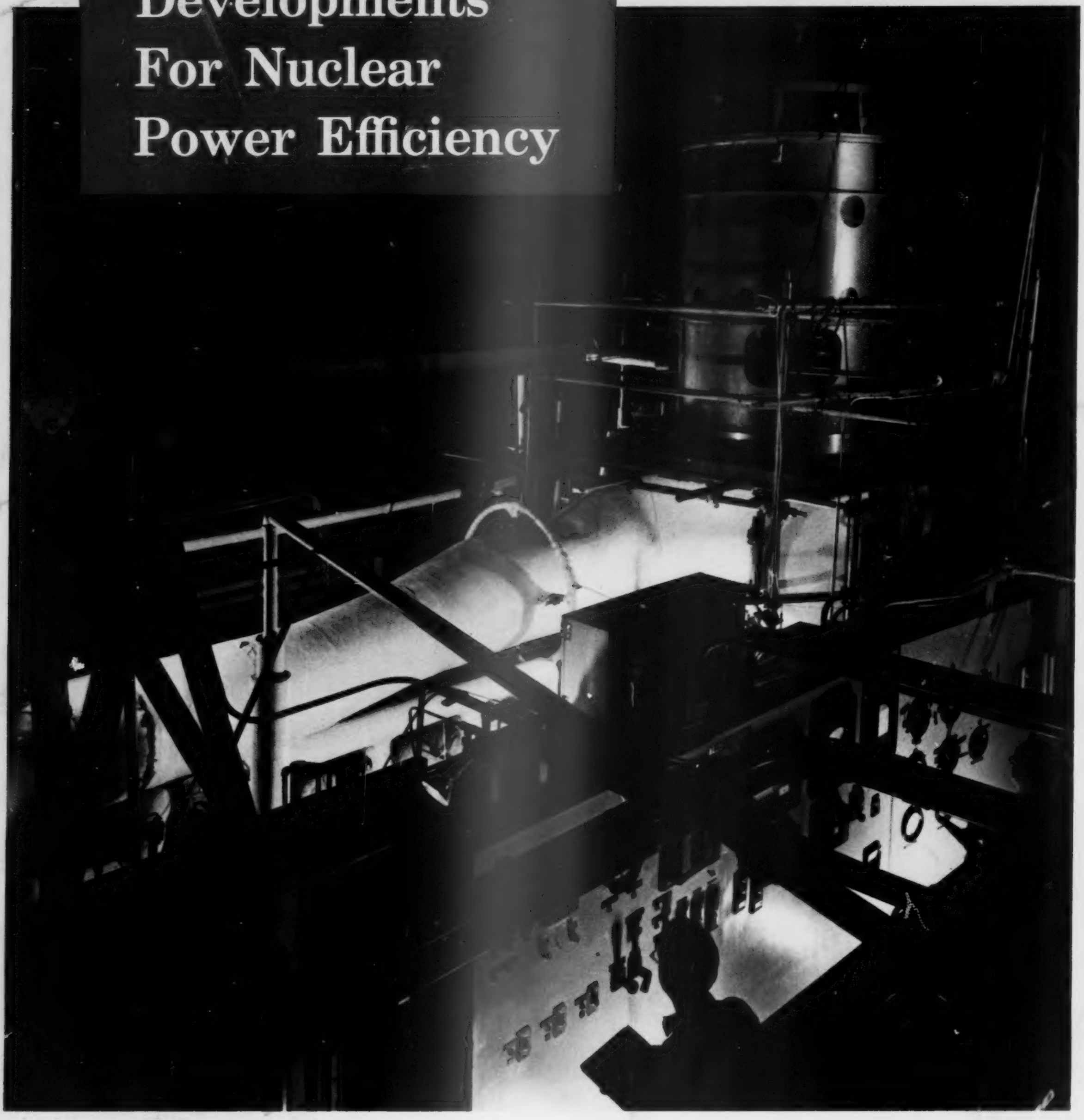
For more information about Allis-Chalmers facilities and capabilities in the nuclear power field, talk to your nearest A-C representative, or write for *Bulletin 43B9541,* **Allis-Chalmers,** Atomic Energy Division, Milwaukee 1, Wisconsin. A-1382

*Central Electric and Gas Co. • Interstate Power Co. • Iowa Power and Light Co. Iowa Southern Utilities Co. • Madison Gas and Electric Co. • Northwestern Public Service Co. • Otter Tail Power Co. • St. Joseph Light and Power Co. • Wisconsin Public Service Corp.

Site of "Pathfinder" plant at Sioux Falls, South Dakota.

FIRST CLASS

Developments For Nuclear Power Efficiency



CONTROLLED LEAKAGE PUMPS for circulating reactor water in the Pathfinder Atomic Power Plant, soon to be in operation, are an excellent example of gains in efficiency. The new pumps have an efficiency 15 points higher than that obtained from canned or hermetically sealed units previously tested for nuclear applications.

Key to the success of the controlled leakage pumps is their buffer type shaft seals, which were the culmination of several years of development work. The seal works on a hydrodynamic balance operating with a gap of less than 0.0001 inch, yet seal components can be manufactured to easily attainable tolerances. Testing was done on this seal with a leakage rate as low as one-tenth gpm of buffer water.

The buffer water serves to prevent any contamination of the seal area by the radioactive water, and to keep the seal cool for maximum life. The seal performs well over the entire speed range as well as during shutdown. Vibration, thermal expansion, alignment, and wear have little effect on the sensitivity of this seal, and it can be used for sealing pressures up to 2000 psig.

The prototype recirculating pump, being tested under simulated field operating conditions at 486 F and 600 psig, is demonstrating its adequacy for the Pathfinder project. Recirculating water for the Pathfinder reactor will require three of these pumps, each with a capacity of 20,000 gpm at a total developed head of 71 feet.

The increase in efficiency in this case stems from the higher efficiency of a conventional vertical pump and motor combination and from the negligible power consumed by the buffer seal. Knowledge gained from the vast amount of development work on the Pathfinder project should result in far greater operating efficiency and also in a considerable reduction in plant cost per kw of plant capacity.

by R. A. HAUGEN
Pump and Compressor Department
Allis-Chalmers Mfg. Co.

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